VOLUME 105 PART A NUMBER 22 AUGUST 1958

The Proceedings

OF

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FOUNDED 1871: INCORPORATED BY ROYAL CHARTER 1921

PART A

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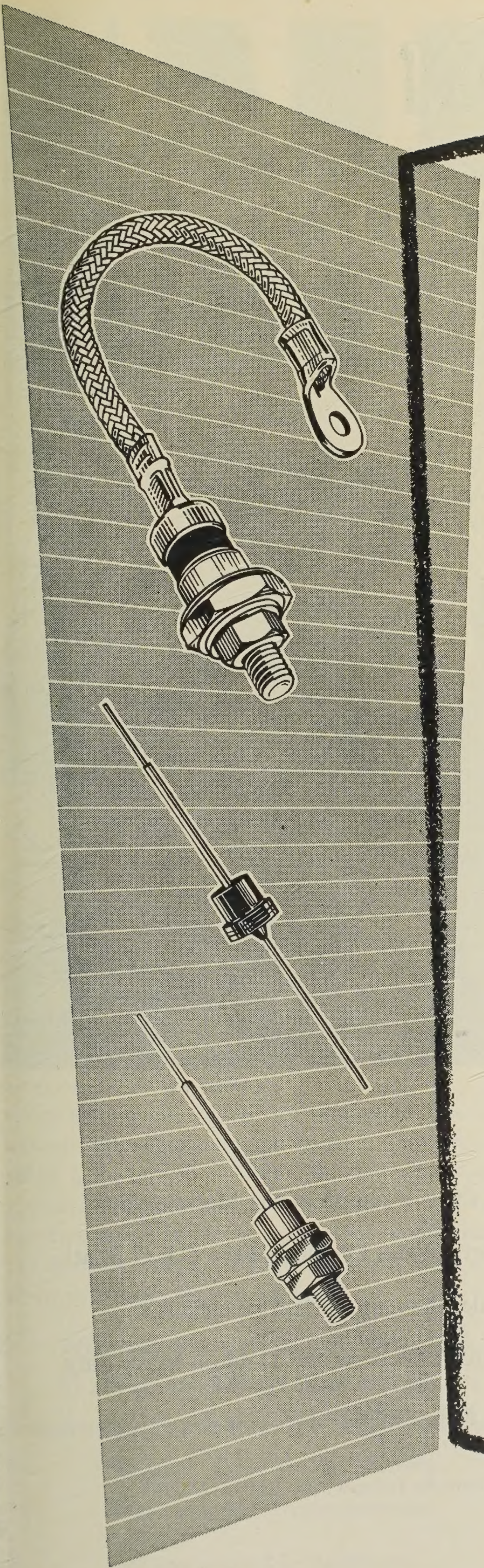

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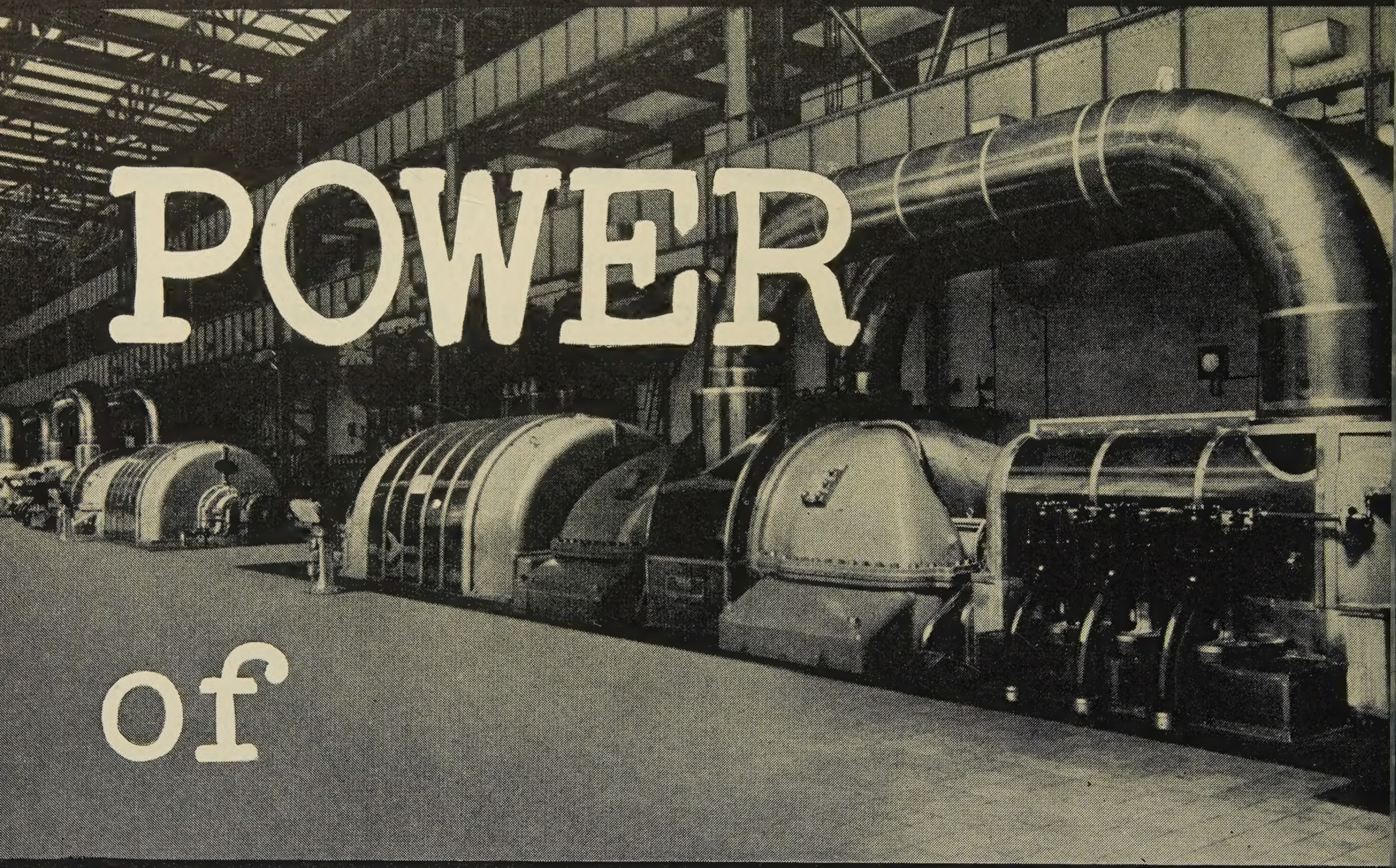

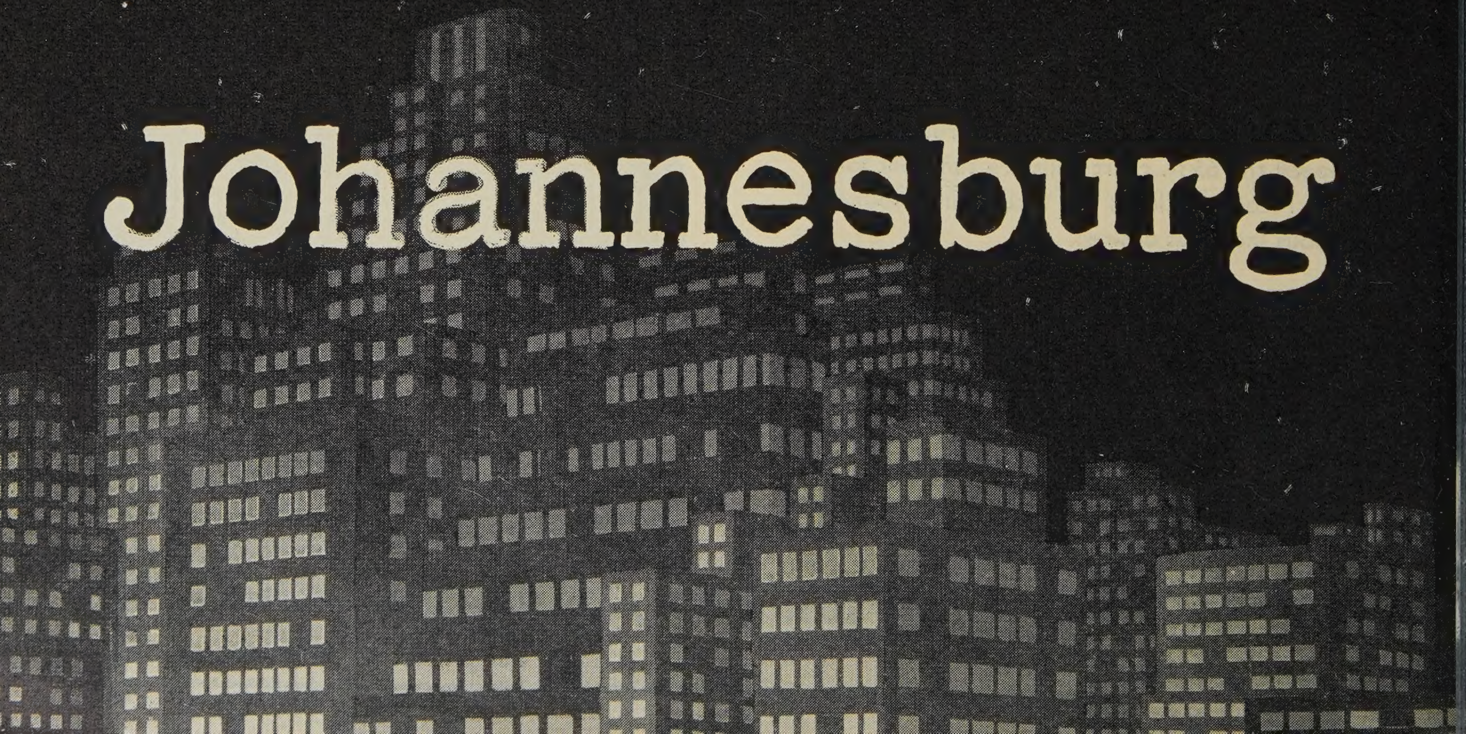

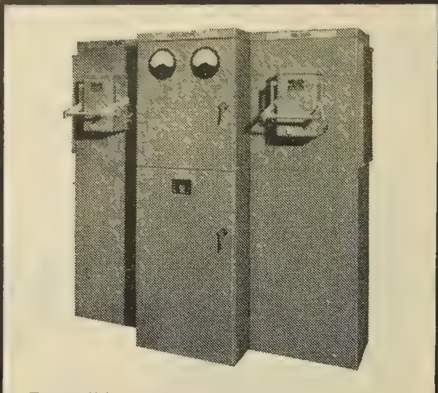

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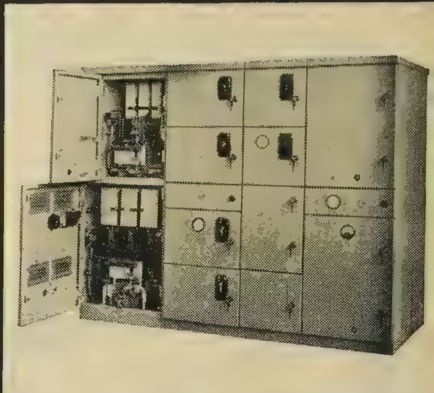

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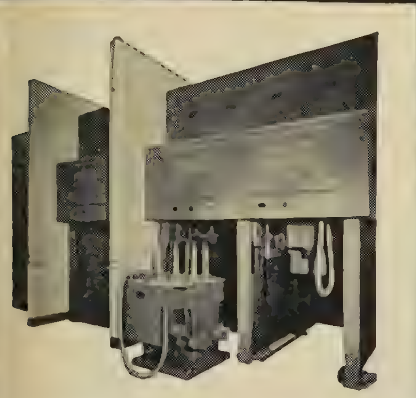

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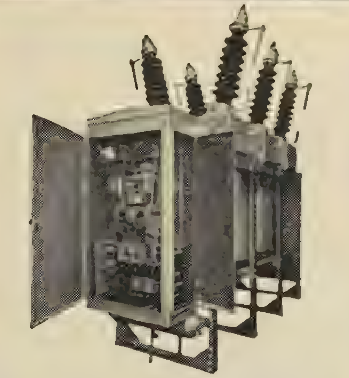

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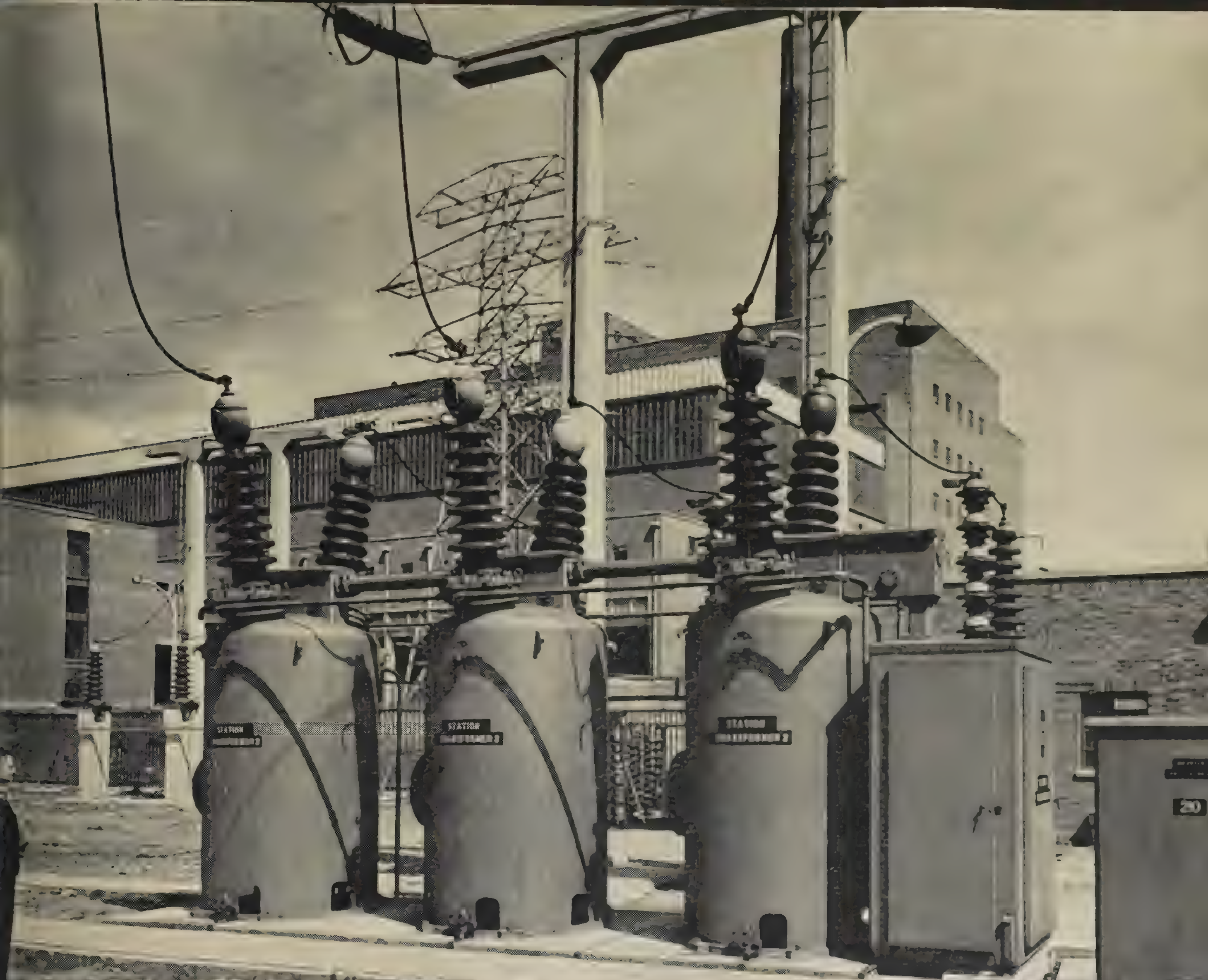

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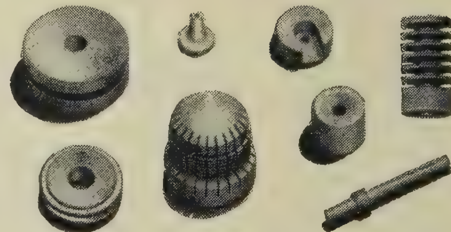

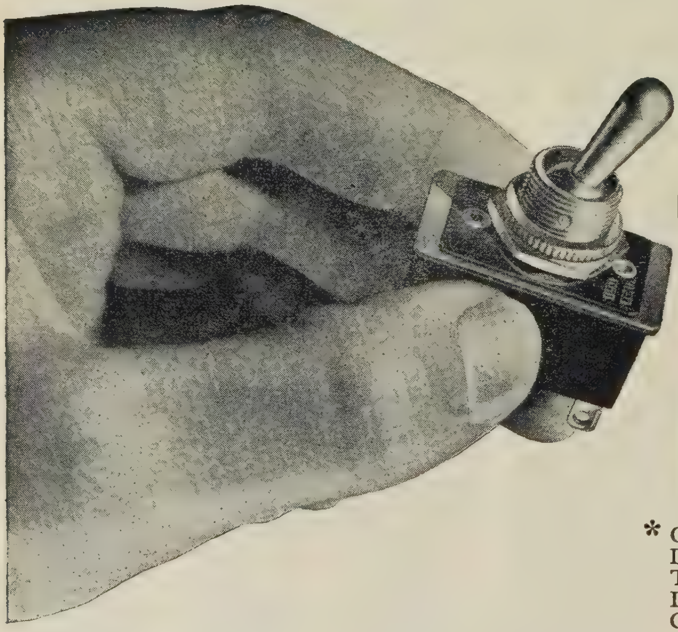

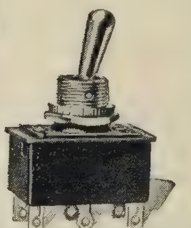

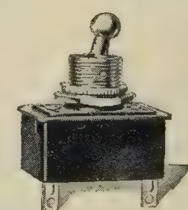

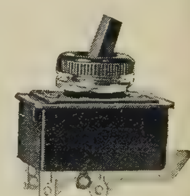

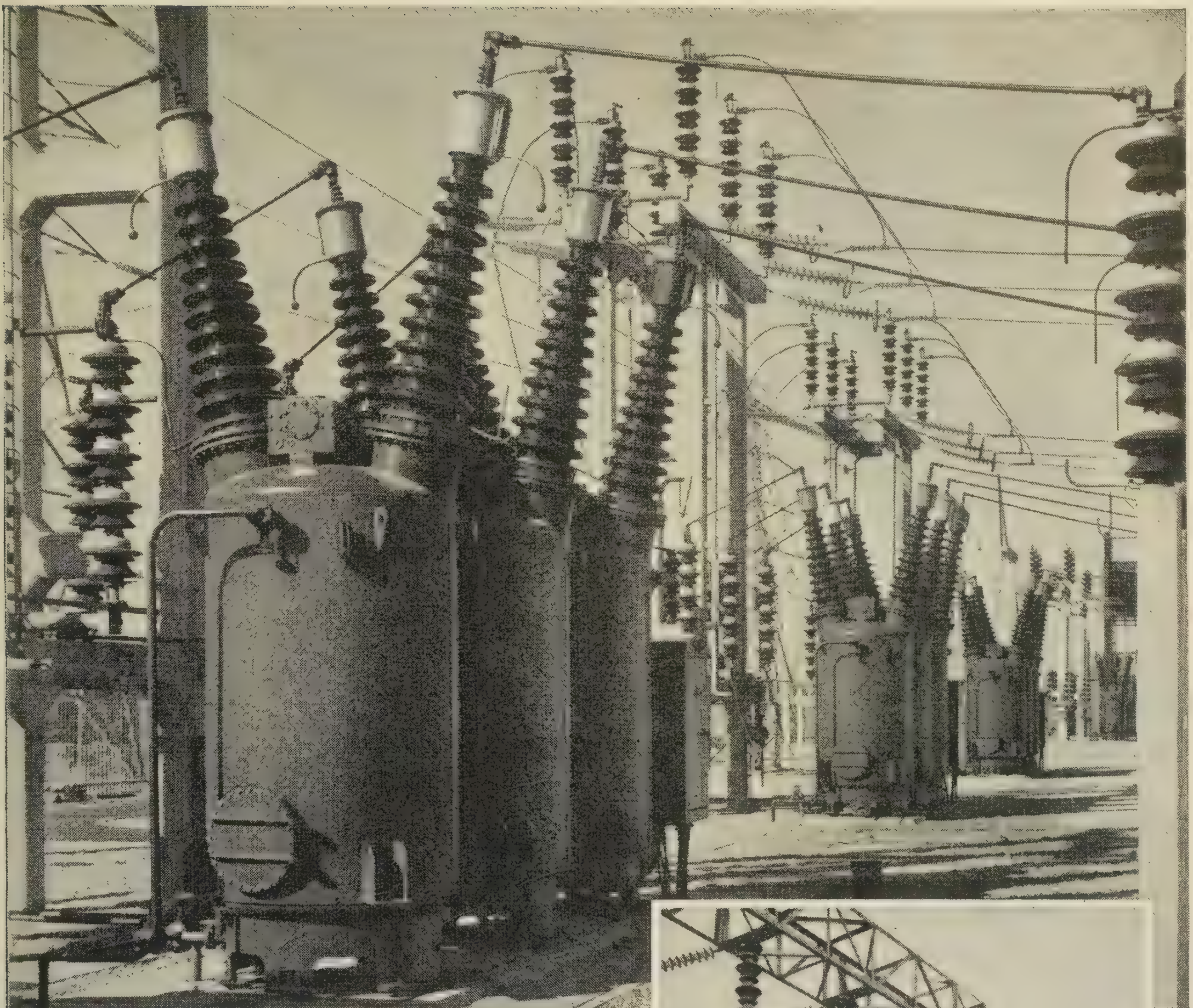

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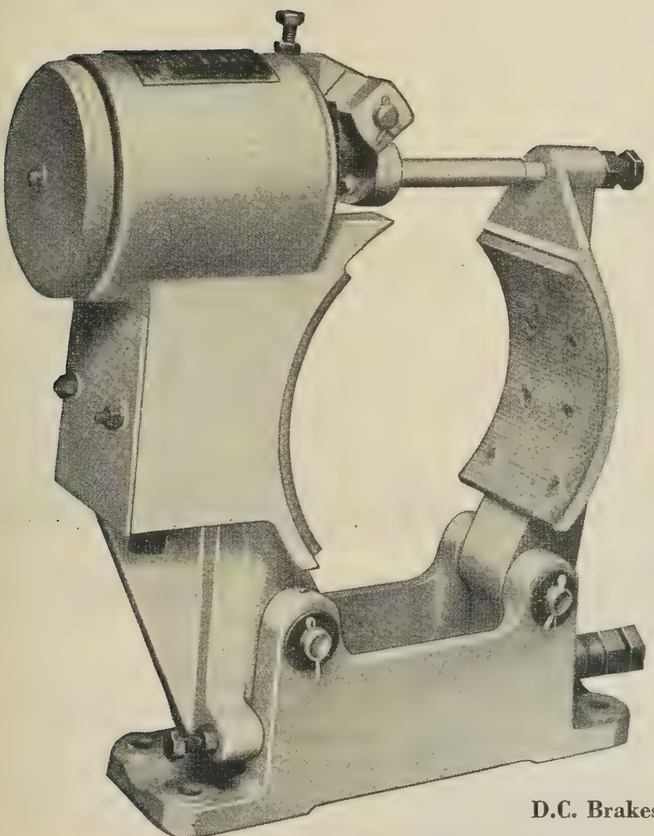

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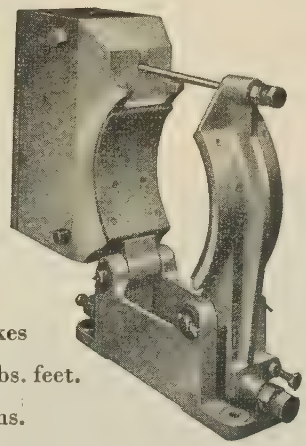

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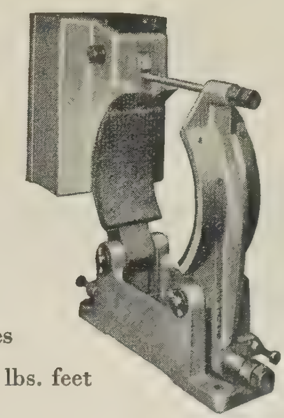

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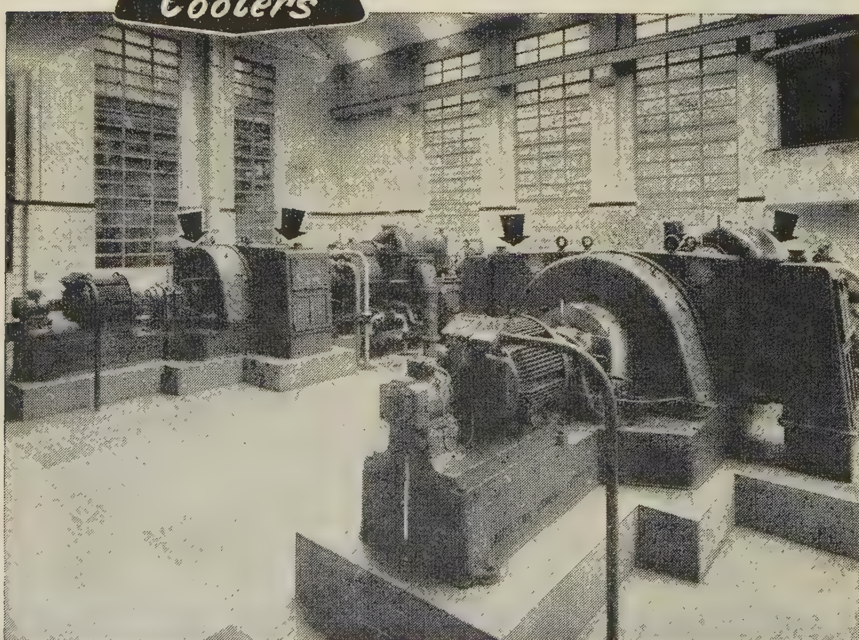

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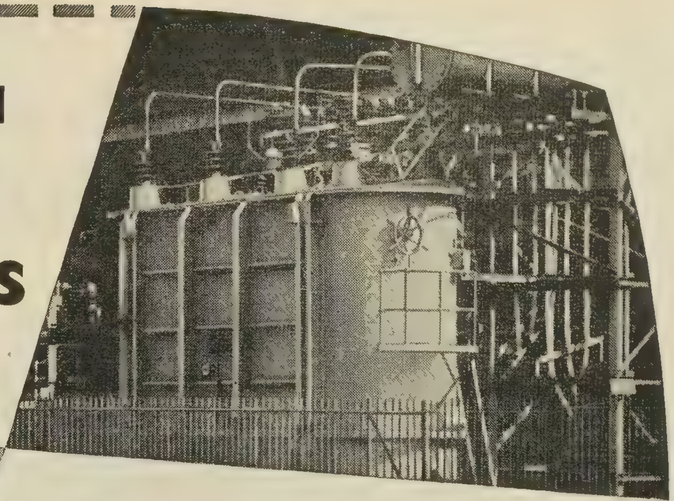

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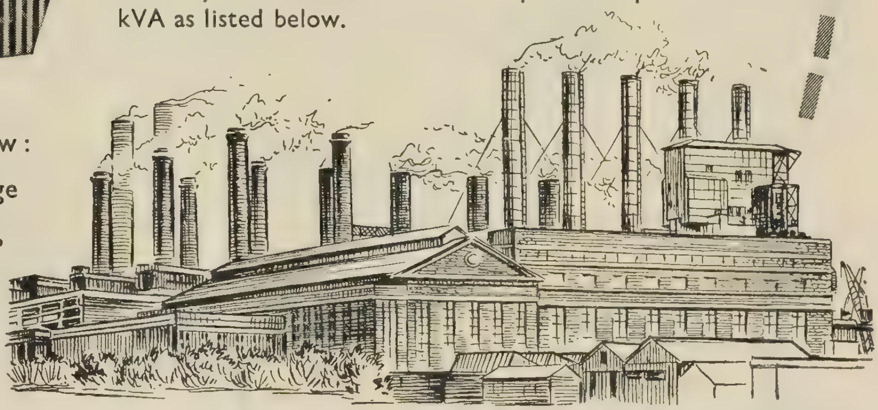

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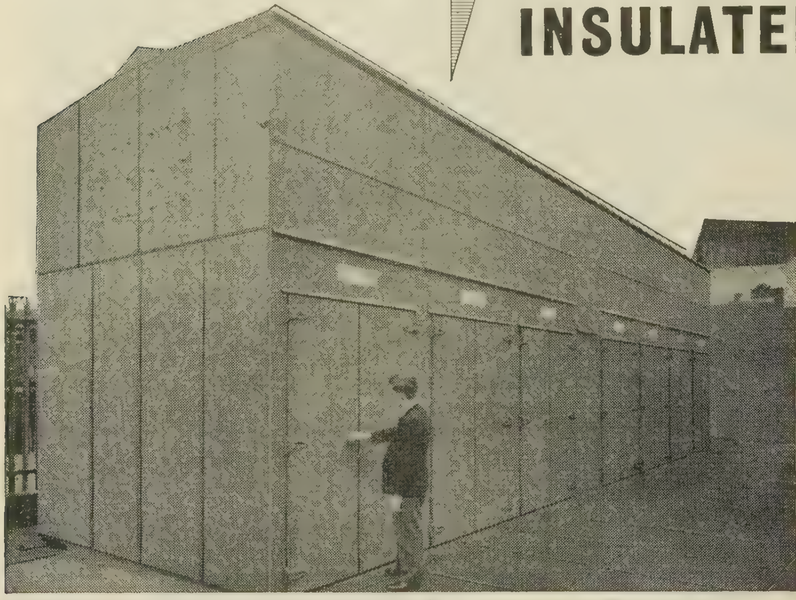


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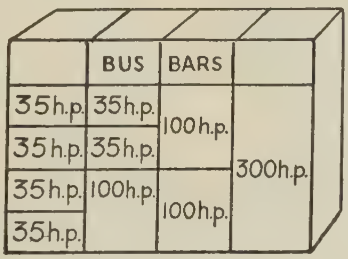

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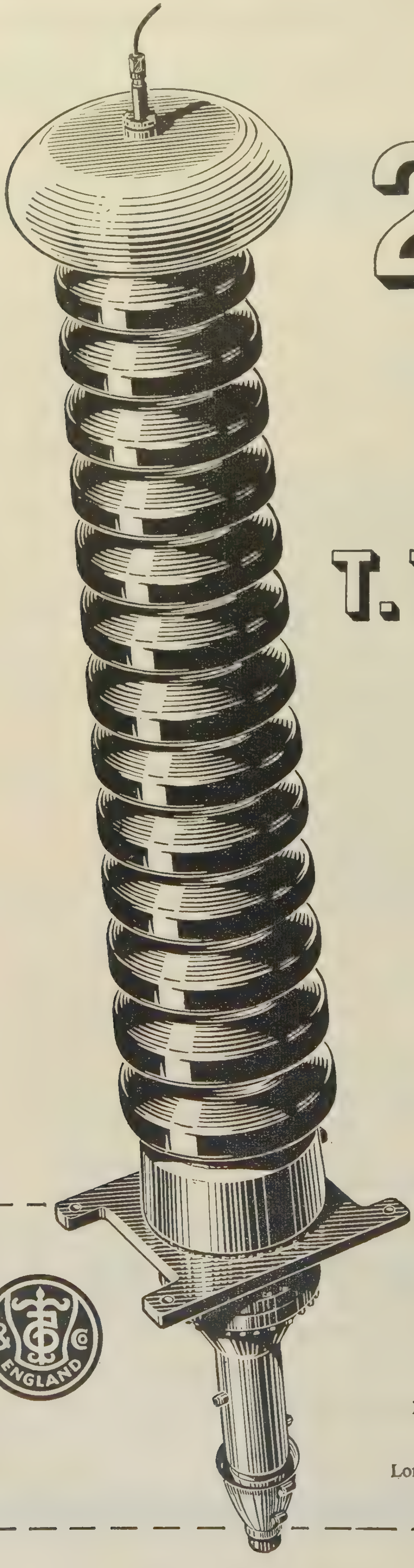

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THE PROCEEDINGS OF
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SUPPLY-VOLTAGE AND CURRENT VARIATIONS PRODUCED BY A 60-TON 3-PHASE ELECTRIC ARC FURNACE

By B. C. ROBINSON, M.Sc., Ph.D., Member, and A. I. WINDER.

(*The paper was first received* 30*th April, and in revised form* 2*nd October*, 1957. *It was published in December*, 1957, *and was read before the* NORTH-EASTERN CENTRE 10*th February, a joint meeting of the* UTILIZATION *and* SUPPLY SECTIONS 13*th February, the* SHEFFIELD SUB-CENTRE 19*th February, and the* NORTH-WESTERN CENTRE 6*th May*, 1958.)

SUMMARY

The use of electric arc furnaces for the production of high-grade steels is becoming increasingly common. At the same time the size of the new furnaces is being increased to permit the melting of larger quantities of metal at a time. An arc furnace does, however, present a number of difficulties to the supply engineer on account of the rapid variations of load which it produces.

The first 60-ton arc furnace in this country was installed at the end of 1954, and it was felt desirable to determine what effect it had on the supply network. The electric power for the furnace was obtained from a temporary connection to the 66 kV system through a 66/11 kV 15 MVA transformer, pending the provision of a permanent supply direct from a 132/66 kV supply point. During tests on the 11 kV line, voltage fluctuations from about 75 to nearly 150% of normal voltage were recorded. These can be shown to be due to unstable arcing conditions in the furnace and to the impedance of the supply network. Calculations indicate that about 17% of these voltage variations were produced on the 66 kV network.

During the initial period of a melt, arcing conditions are very unstable owing to the conditions in the furnace. Initially the metal is cold and forms a poor arcing electrode. Also, since the charge is composed of scrap material it is liable to collapse from time to time, short-circuiting an arc.

Alternatively, owing to a previous over-current surge the electrode control-gear may withdraw the electrode too far, causing the arc to be extinguished. These initial fluctuations may take place several times a minute with an amplitude of several hundred amperes. As the charge begins to melt the power input is increased, and records show that unbalanced currents of about 1 500 amp may occur on the 11 kV system.

Measurements indicate that the actual voltage drop across the arcs between the electrodes and the charge may vary between about 110 and 400 volts, depending on the furnace conditions.

Since the various voltages in the circuit are all magnetically coupled they are uniformly distributed through the various windings, and no special reinforcement of the transformer end-turn insulation is required, as in the case of lightning surges.

LIST OF PRINCIPAL SYMBOLS

I_R, etc. = Phase current in substation transformer (11 kV).
i_{RY}, etc. = Phase current in furnace transformer (11 kV).
k = Arc-furnace transformer ratio.
L_1 = Equivalent inductance of substation transformer.
L_2 = Equivalent inductance of furnace transformer and leads.
V = Arc-voltage drop.
V_S = Supply voltage.
$(V_S)_{RY}$, etc. = Supply-voltage phase RY $[= V_S \sin(\omega t + \phi)]$, etc.
v_{RY}, etc. = Arc voltage in phase RY referred to the 11 kV winding.
$(v_R)_R$, etc. = Reactance drop in substation transformer phase R.
$(v_T)_R$, etc. = Terminal voltage of substation transformer to earth (phase R).
$(v_T)_{RY}$, etc. = Terminal voltage of furnace transformer (phase to phase) (phase RY).
ϕ = Phase angle between arc-voltage phase R and supply-voltage phase RY.

(1) INTRODUCTION

Electric arc furnaces are now being employed on an increasing scale for the production of steel. Originally they were only used for the production of high-grade alloys. However, with the development of large furnaces it is becoming an economic proposition to use them for making commercial steels.

From the supply engineer's standpoint an electric arc furnace forms a heavy load which is not without its difficulties. These have been the subject of several papers, particularly in America, where arc furnaces are commoner than in this country. Most of these papers appear to be concerned with one of two troubles —the presence of abnormal voltages and the flickering of lights in the neighbourhood owing to variations of supply voltages. In a number of cases these over-voltages have been attributed to switching surges.[1, 2, 3, 4] There is also some evidence that over-voltages occurring in arc furnaces may be connected with other aspects of furnace operation such as arc-current interruption.[3] One of the authors has also shown in a previous paper[5] that in some arc furnace units excess voltages may be produced by connecting transformers and series reactors in the wrong order relative to the supply-voltage phase sequence.

The problem of the light flicker produced by electric arc

Dr. Robinson and Mr. Winder are with C. A. Parsons and Co., Ltd.

furnaces has been the subject of several papers in United States.[6–11] From these it would appear that the maximum sensitivity of the eye to flicker occurs with a frequency of about 6–8 c/s, when some 50% of the people tested can detect a periodic change of about 0·5% in the voltage supply and over 90% of people can detect a 1% change in the supply voltage to a tungsten lamp. Discharge lamps are less sensitive to flicker-voltage variation. This flicker appears to be caused by the regulation voltage drop in the supply network as the current taken by the furnace varies. The authors themselves have noticed lamp flicker produced by the furnace they were testing when they were several miles away. According to Ramsaur and Treweek[9] the system

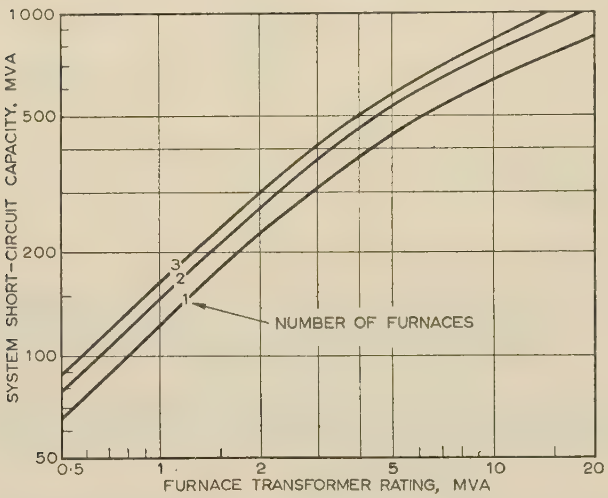

Fig. 1.—Relation between arc-furnace transformer rating and recommended minimum supply-system short-circuit capacity [Ramsaur and Treweek[9]].

short-circuit should not be less than the value given in Fig. 1. It appears that American practice is to work to about $1\frac{3}{4}$% flicker voltage.

(2) ARC-FURNACE INVESTIGATIONS

(2.1) Description of Plant and Equipment

Towards the end of 1954 a new arc furnace was completed at Stocksbridge which was designed for a charge of 60 tons of steel, though later experience has shown that pours of 70 tons are easily obtainable. This was the largest furnace of its type in the country and probably in Europe. As this size of furnace was likely to be installed elsewhere if the operation of the first unit was found to be technically and economically successful, it was felt desirable to investigate the likelihood of any excess voltages occurring at its terminals and thus being fed into the supply network. It would also be possible to decide whether any protective measures were desirable.

The general construction of the furnace follows the usual lines and has been described in detail elsewhere,[12, 13] but a brief description is included to assist in the appreciation of the problems involved. The furnace itself consists of a large steel crucible lined with refractory material and having an inside diameter of about 16 ft. The removable lid is made of similar material and is arranged so that it may be raised and swung to one side to enable the furnace to be charged with scrap. This is placed in the crucible from a large hopper or bucket with a removable bottom.

The three arcing electrodes are 20 in diameter and enter the furnace through holes in the roof. They extend down to the charge itself, and the arcs take place between the electrodes and the charge. The electrodes are raised or lowered on to the charge, either manually or by automatic control which is actuated by the electrode voltages and currents.

The electric arcs form a peculiar load, since they have a negative impedance which causes the current to increase as the voltage decreases. In order to obtain stable operation, it is necessary to have a positive impedance in the circuit. In the case of the furnace considered, this is provided by the inherent inductance of the furnace transformer and its leads. In other installations it may be necessary to add reactance in the transformer h.v. circuit.

The transformer supplying the power to the arcing electrodes of a furnace is situated as close as possible to the furnace on account of the heavy secondary currents involved. The l.v. winding of the transformer is usually delta-connected to reduce the actual winding currents. The h.v. winding may be either star- or delta-connected and usually contains some form of tapping winding to enable the secondary voltage to be varied as demanded by the furnace conditions or power required.

In the plant investigated the furnace transformer had an 11 kV primary winding and gave a variable output of 90–325 volts which was directly connected to the furnace electrodes through multiple flexible leads. The 'transformer' itself was really two transformers, as shown in Fig. 2. The first was connected delta-star with a secondary winding giving a variable output of 6·092–22 kV through on-load tap-changing gear. The second transformer was connected star-delta and had a fixed ratio of

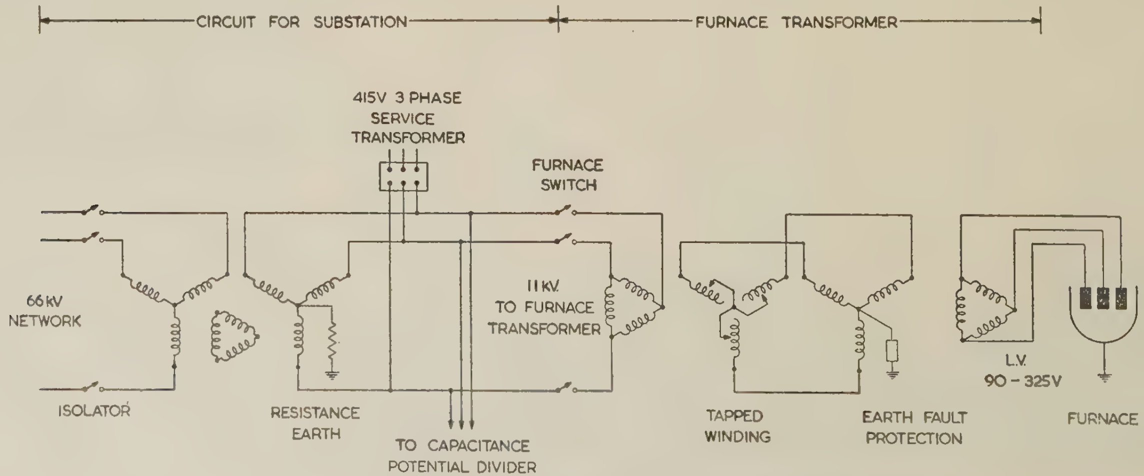

Fig. 2.—Circuit diagram for substation and arc furnace during tests.

22 kV to 325 volts. The transformer was supplied from a tee line on the 66 kV feeder through a 15 MVA 66/11 kV star-star step-down transformer. Whilst the tests described in the paper were in progress, arrangements were being made to transfer the tee line to a new substation being built on a neighbouring 132 kV Grid line. The furnace auxiliaries were all operated from the works 11 kV ring main, which was fed from another substation.

The authors carried out two series of tests on the above plant to try to measure any excess voltages which might be produced by the furnace and to obtain data which might assist a theoretical investigation to explain these voltages. It was only possible to make connections to the circuit at the transformer terminals or other exposed points on the system, so any measurement of internal voltages was precluded. Examination of the circuit showed that the most convenient point to measure any excess voltages which might occur on the supply network was at the 11 kV terminals of the 66/11 kV star-star transformer. Since this winding is earthed any excess voltages appearing on the terminal may be considered in terms of voltages to earth. The 11 kV system had the added advantage that the ratio of the potential dividers required would be lower. Capacitance potential dividers were chosen in preference to voltage transformers on the grounds of convenience and frequency response. The h.v. capacitors were made from six units of 1 200 pF each with a surge working voltage of 15 kV in series. This conformed with the general line insulation level of 90 kV.

As the voltages to be measured were likely to occur at random and were of unknown frequency and magnitude, it was decided that the most suitable instrument for measuring them was a continuously-recording cathode-ray oscillograph. It was therefore decided to build a four-tube instrument and to deflect the beams along the line of tubes. The traces thus produced were photographed on a continuously-moving film whose direction of movement was perpendicular to the axis of deflection. This gave the records as a band whose width was proportional to the oscillograph deflection and thus to the voltage on the deflection plates. Two film speeds of about 3 in/min and 4 ft/min were tried, but the latter was found to be more suitable as it permitted every cycle of the supply voltage to be inspected and thus enabled the duration of the surges to be ascertained. Three of the oscillograph tubes were connected to record the voltages to earth on the three 11 kV phases. The fourth tube was connected through an amplifier to a shunt resistor energized from the secondary winding of a current transformer on the yellow phase. Arrangements were also made to short-circuit this shunt mechanically once every minute to provide a time scale.

In addition to these low-speed records a number of oscillograms using an ordinary oscilloscope were taken to show the waveforms of the 11 kV voltage to earth and the phase current. It was hoped that these would provide confirmatory evidence for any theoretical analysis which might be attempted.

A second series of records were taken of the current in each of the 11 kV lines using a 3-phase recording ammeter with a chart speed of 3 in/min. A synchronized time marking on the charts permitted these records to be correlated with the voltage oscillograms.

During one of the melts in the furnace, records were taken on both the moving-film oscillograph and the oscilloscope of the voltage between the furnace electrodes and the bath. It was not considered practicable to connect to the actual charge in the furnace, but connection was made to an electrode permanently embedded in the furnace lining. This was used to provide a reasonable conducting path from the charge to the earthed casing of the furnace, particularly when the furnace was hot, and to provide an earth reference point for the electrode-charge voltage for the operation of the electrode control gear. It was hoped to obtain from these measurements an approximate value for the furnace arc-voltage drop for use in theoretical analyses, and at the same time to obtain some evidence of the general behaviour of the arcs during the unstable melting process.

Experience during the tests showed that the furnace operation was unstable for the first two to four hours of each melt, and then fairly steady conditions were maintained during the remainder of the time. It was usual to put a second basket of scrap into the furnace when the first was melted, i.e. after about $1\frac{1}{2}$ or 2 hours. It was during the unstable period that the abnormal voltages were observed. In the authors' opinion this precluded the possibility of the surges being caused by switching operations on the connected network.

The unstable arcing conditions observed at the beginning of the melt can be attributed to a number of causes. Probably the primary factor was that the cold charge provides a very poor arcing electrode owing to its poor electron emissive properties and to the rate at which it will conduct the heat away from the arc itself. Another cause is that initially the charge will melt round the tips of the electrodes, which gradually bore down into the scrap. When the walls of these holes become too high they collapse on to the electrode, causing a short-circuit on that arc. The automatic electrode control-gear will then withdraw the electrode and may cause extinction of the arc if it over-runs. Alternatively, an arc may be extinguished through a collapse of the charge beneath it. Of course, various intermediate arcing conditions exist with varying arc-voltage drops and currents, depending on the length of the arc, the temperature of the electrodes and their thermal conductivity. Stable arcing conditions are obtained when the charge has melted, and, being hot, it forms a good thermionic emitting surface.

(2.2) Test Results

It is proposed to discuss the various phenomena which the tests have demonstrated in separate Sections rather than to deal with each type of recording separately.

(2.2.1) Voltage Variation with Load.

As already mentioned the furnace was fed through a 66/11 kV transformer connected to a temporary tee on the 66 kV network. As a result of this, the effective impedance between the 11 kV busbars and the power stations connected to the system was about 15%, which was nearly all reactive. The greater portion of this impedance was, of course, in the 66/11 kV transformer. Variations of about 500 amp through this impedance will produce voltage variations of 2–3% of the supply voltage. This variation of voltage with current is clearly shown in Fig. 3, where variations of about 11% took place. The figures on this record, as in other charts and records given in the paper, indicate the time from the beginning of the melt. It is perhaps interesting to note that the frequency of the fluctuations is about 75 cycles per minute. Voltages as low as 75% of the open-circuit voltage were recorded occasionally during the tests. This voltage drop is also probably partly due to the influence of the arc waveform in a manner similar to the excess voltages discussed later.

(2.2.2) Excess Voltages.

The principal object of the investigation was to determine the frequency and magnitude of any excess voltages which might occur. The total recording time was about 24 hours, divided over six melts. It was mainly concentrated on the first two or three hours of the melts, as this was the period when most excess voltages were observed to occur. Fig. 4 shows a curve giving the total number of excess voltages over a certain value as a function of the surge voltage. Most of the surges were observed

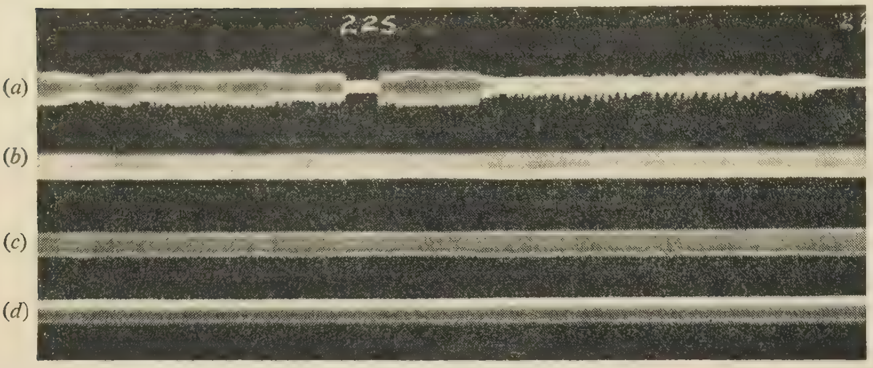

Fig. 3.—Voltages and current on the 11 kV supply to the arc furnace transformer, showing variation of voltage with current.

(a) Current in yellow phase.
(b) Voltage to earth, blue phase.
(c) Voltage to earth, yellow phase.
(d) Voltage to earth, red phase.

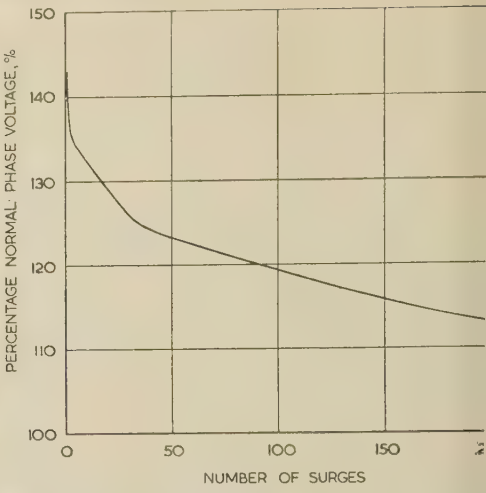

Fig. 4.—Number of surges which exceeded a giver percentage of the normal transformer open circuit voltage.

to last for about one-half or one cycle. Fig. 5 is a record showing a large number of excess voltages occurring in a short period. The majority of these are small in magnitude.

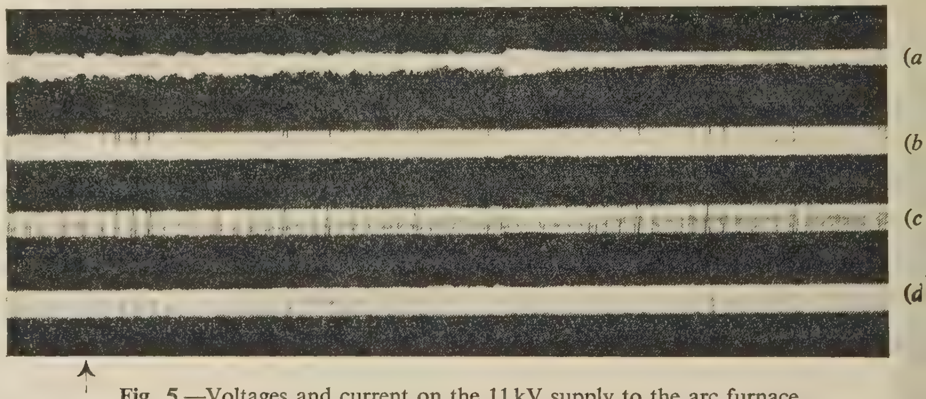

Fig. 5.—Voltages and current on the 11 kV supply to the arc furnace transformer showing various over-voltages.

The arrow indicates a surge of 143% normal voltage on record (c).
(a) Current, yellow phase. (c) Voltage to earth, yellow phase.
(b) Voltage to earth, blue phase. (d) Voltage to earth, red phase.
Time: 14 min 53 sec. Duration of oscillogram: 7 sec.

(2.2.3) **Ammeter Records.**

The ammeter records of the currents in the three phases of the 11 kV supply give some interesting information regarding the variations of load between the phases with time. The nature of the records, of course, preclude any detailed cycle-by-cycle study. Fig. 6 shows a typical record taken during the early stages of a melt. It will be seen that the currents vary rapidly over a range of several hundred amperes.

Fig. 7 was taken later on in a melt when the current had increased to its maximum. This record shows how two arcs may be carrying a much heavier current than the third, and how the heavy current may change from one phase to another (in this case from blue to red) without appreciably affecting the third (yellow phase).

Fig. 8 shows a period with heavy currents of nearly 1 500 amp followed by the extinction of one of the arcs (blue phase). About a minute after this arc has restruck, the arc in another phase is extinguished.

(2.2.4) **Arc Voltages.**

A number of oscillograms were taken of the arc waveform to see how closely it approximated to the ideal rectangular shape assumed in the theoretical investigations, and to obtain an approximate value for the arc-voltage drop when plotting the theoretical curves. Typical examples are shown in Fig. 9, together with approximate values of the peak voltage.

The oscillograms indicated that the maximum value of the equivalent rectangular voltage was about 190 volts, but peak values of about 410 volts were obtained owing to irregularities in the waveform.

The oscillograms show a wide variety of waveshapes, from the more or less rectangular shape in Fig. 9(*a*) to the more complicated shapes. Fig. 9(*c*) shows a distinct unsymmetrical waveform. The pointed half-wave occurred with the electrode positive with respect to the charge, which would conform to the condition that a cold charge would provide a poor electron emitting surface. Figs. 9(*d*) and 9(*e*) show variations occurring from cycle to cycle. Fig. 9(*e*) is particularly interesting as it shows the commencement of a short-circuit from the electrode to the charge.

Fig. 10 shows an oscillogram of the transformer l.v. terminal corresponding to Fig. 9(*e*). It will be seen that the voltage here does not drop appreciably when the short-circuit occurs. The additional voltage difference between the two oscillograms after the short-circuit is dropped in the reactance of the leads betweer the transformer terminal and the electrode clamp by the additiona current flowing.

The oscillograms indicate that the mean voltage drop corre sponding to a rectangular waveform between the electrode clamp and the charge was about 110–200 volts. This formed the basis of the value of 190 volts assumed for the theoretical waveforms The maximum peak value measured was about 410 volts, whicl would correspond to $325\sqrt{(2/3)}$ or the voltage between ar electrode and the bath when the arc between them was extin guished under 2-phase arcing conditions.

A moving-film oscillogram of the arc voltage, showing the variations in voltage soon after switching on, is shown in Fig. 11 It will be noted that considerable variations in arc voltage take place. It appears that single arcs are extinguished several time

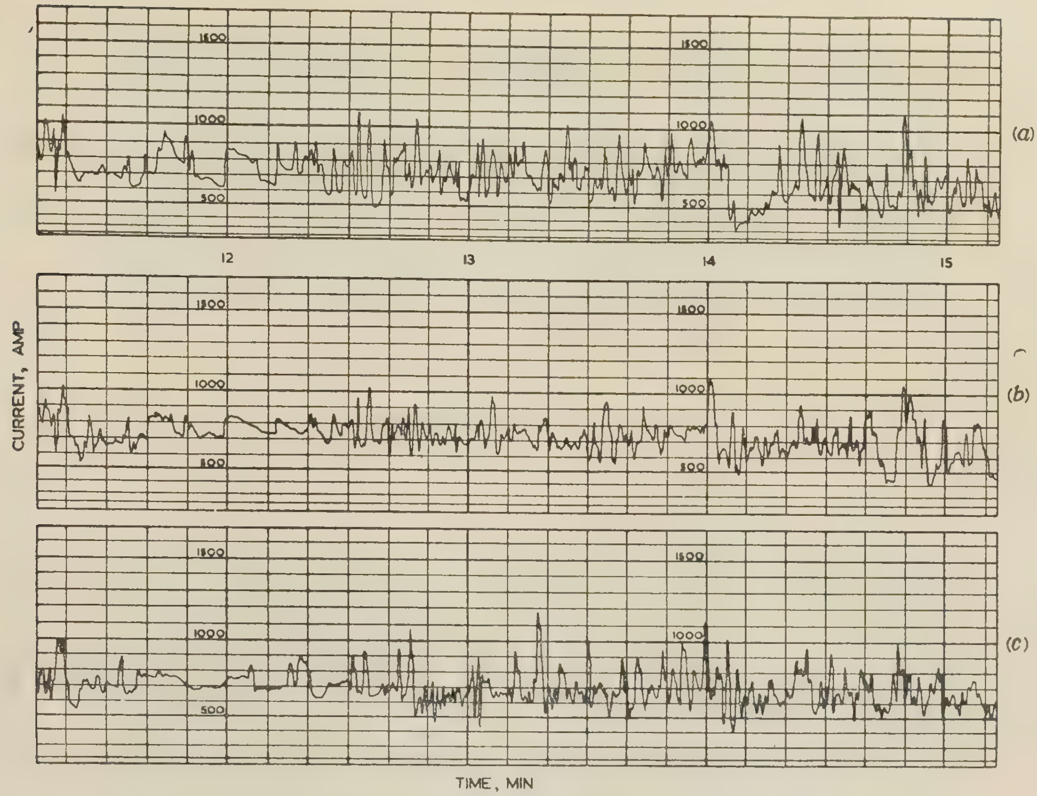

Fig. 6.—Chart from recording ammeter on the 11 kV supply to the arc-furnace transformer.

(*a*) Blue phase. (*b*) Yellow phase. (*c*) Red phase.

The time is in minutes from the commencement of the melt.

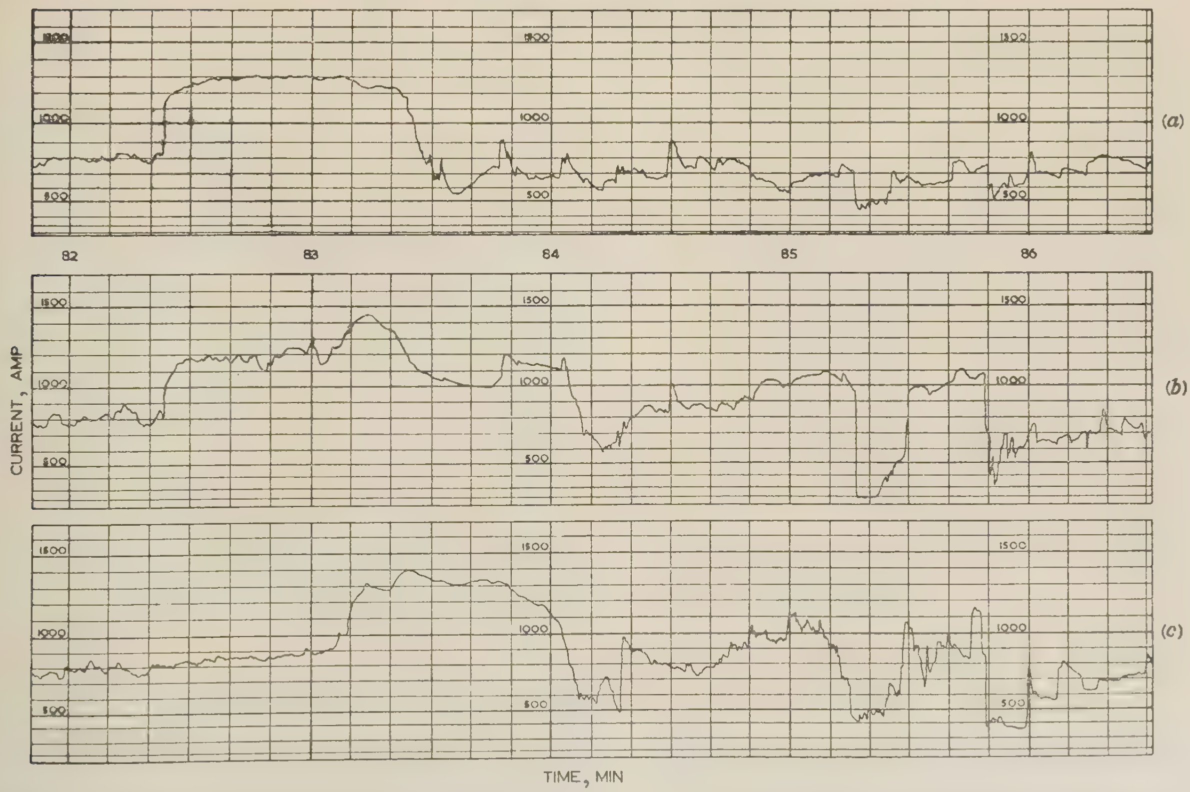

Fig. 7.—Chart from recording ammeter on the 11 kV supply to the arc-furnace transformer, showing large current variations.

(*a*) Blue phase. (*b*) Yellow phase. (*c*) Red phase.

The time is in minutes from the commencement of the melt.

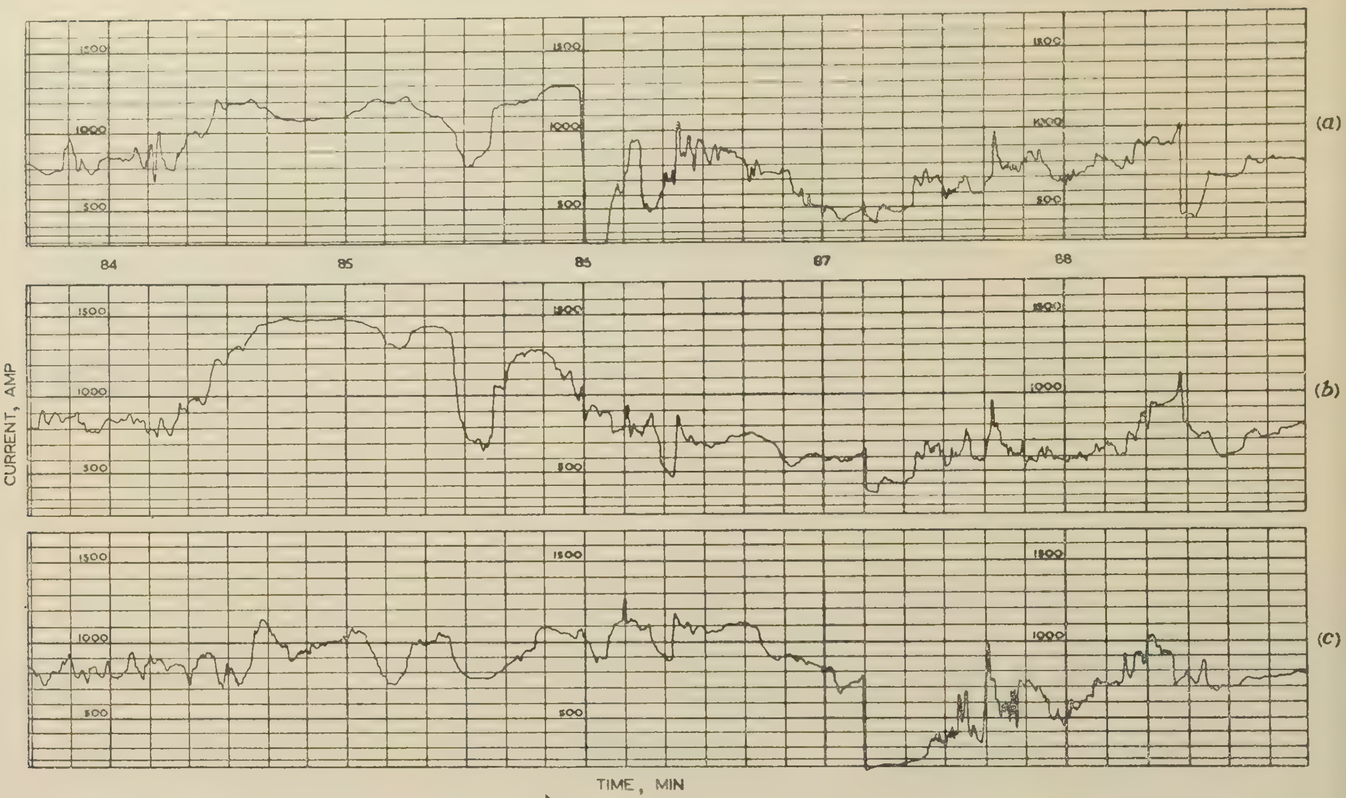

Fig. 8.—Chart from recording ammeter on the 11 kV supply to the arc-furnace transformer, showing large current variations.

(*a*) Blue phase. (*b*) Yellow phase. (*c*) Red phase.

The time is in minutes from the commencement of the melt.

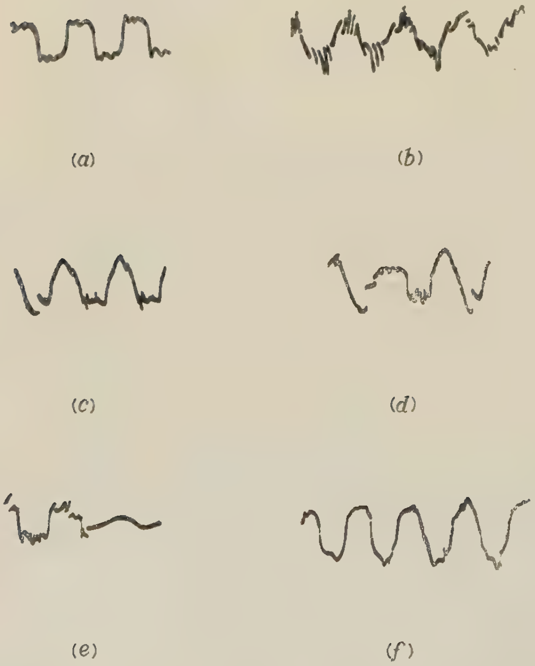

Fig. 9.—Typical arc voltage waveforms.

The voltages are recorded between the electrode clamp and the electrode buried in the furnace lining.

Maximum voltages of oscillograms (peak to peak)

(*a*) 450 volts. (*b*) 740 volts.
(*c*) 600 volts. (*d*) 690 volts.
(*e*) 540 volts. (*f*) 780 volts.

Fig. 10.—Voltage at furnace transformer terminal corresponding to Fig. 9(*e*).

The difference between these oscillograms represents the voltage drop in the leads between the transformer and the electrode clamps.

during the period covered by this record. These are due to short-circuits to the charge and are probably caused by the scrap metal collapsing on to the electrodes. A small voltage (39 volts) is shown on the oscillogram during these short-circuits; this is due to electrode and charge-impedance voltage drop and to induced voltages in the measuring leads. A typical record of an unsteady period of arcing which was taken nearly half an hour later is shown in Fig. 12. One characteristic of the arc-voltage variations is clearly shown in the top trace. The arc voltage drops suddenly from what might be considered to be a normal value to a low value, but its increase to the previous value takes place comparatively slowly. This is probably due to a collapse of the charge causing a sudden shortening of the arc length, followed by the slower movement of the electrode control-gear to compensate for the change.

(3) THEORETICAL EVALUATION OF THE VOLTAGES ON THE 11 kV BUSBARS

It is possible to analyse the furnace supply circuit and obtain expressions for the voltages on the 11 kV network if certain assumptions are made as below. Some of these assumptions are similar to those made in a previous paper on arc furnace voltages.

(*a*) The arc waveform is rectangular.

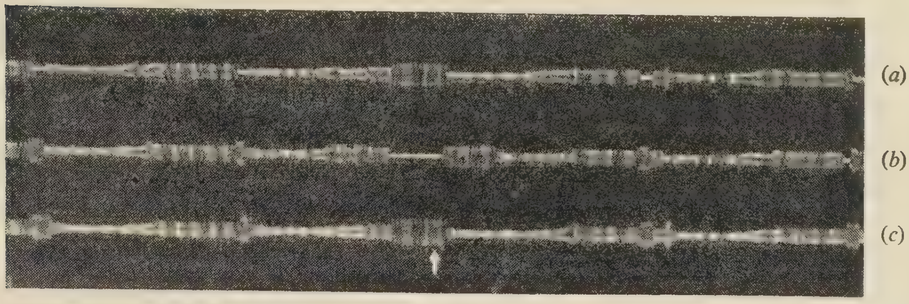

Fig. 11.—Oscillogram showing variation of arc voltages soon after switching on (time, 3 min 5 sec).

Voltages are taken at the arrow.

(a) Red phase: 225 volts.
(b) Yellow phase: 39 volts.
(c) Blue phase: 166 volts.

Duration of record: about 7 sec.

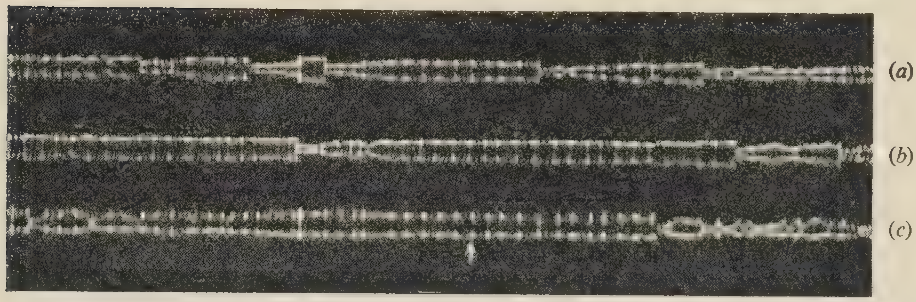

Fig. 12.—Oscillogram showing variation of arc voltages 27 min after switching on.

Voltages are taken at the arrow.

(a) Red phase: 226 volts.
(b) Yellow phase: 228 volts.
(c) Blue phase: 179 volts.

Duration of record: about 7 sec.

(b) The arc restrikes in the opposite direction immediately it is extinguished in the first direction.

(c) The furnace lead reactance and the furnace transformer reactance may be represented by lumped reactors in the delta loop of the transformer.

(d) The 66/11 kV substation transformer reactance may be represented by a reactor in series with each of the l.v. windings.

(e) By Thévenin's theorem the impedance of the network is the impedance between the substation transformer and the various power stations supplying the system in parallel. It is necessary, for simplicity, to ignore the shunt impedances of the various loads on the network, but these are relatively large compared with the line impedances. This impedance was assumed to be in series with the substation transformer reactance.

(f) The furnace transformer may be treated as a single delta-delta unit of suitable ratio.

It is also necessary to assume certain simple operating conditions of the furnace as follows:

(i) Three-phase arcing from all three electrodes.

(ii) Two electrodes arcing and the third electrode short-circuited to the charge.

(iii) Two electrodes arcing and the third electrode open-circuited to the charge.

A detailed analysis of the circuit is given in Section 8. The calculated waveforms are given in Figs. 13–15, together with oscillograms taken during the tests, which exhibit a similarity indicating that similar conditions must have existed. Dotted horizontal lines on the curves indicate the peak of the supply voltage [assumed to be 11·6 kV (r.m.s.)]. The other dotted lines indicate the effect of increasing the peak of the arc voltage to

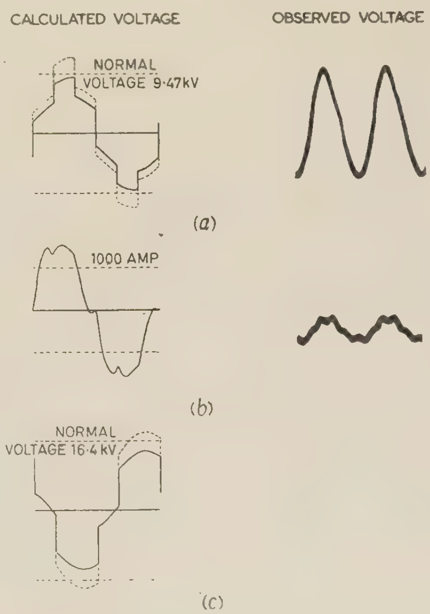

Fig. 13.—Voltages and currents with 3-phase arcing.

(a) Substation transformer voltages to earth.
(b) 11 kV phase currents.
(c) Furnace transformer voltages between phases.

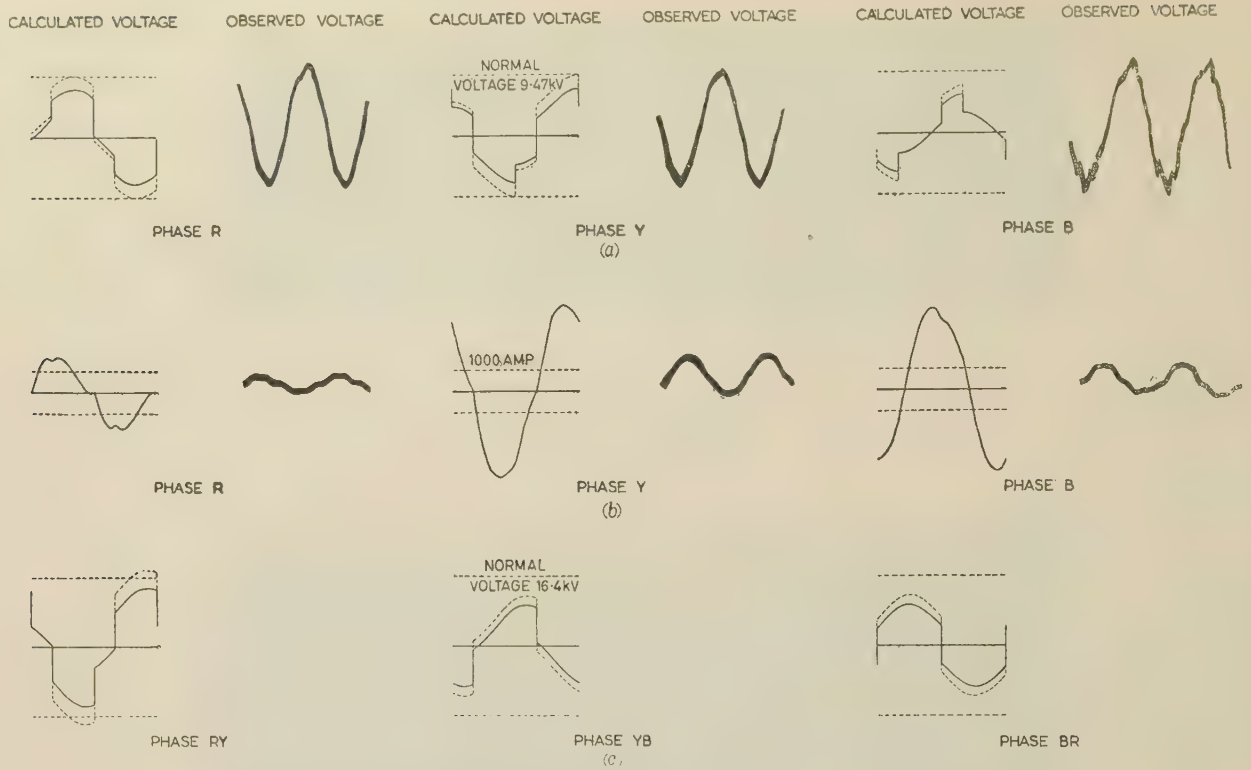

Fig. 14.—Voltages and currents with 2-phase arcing, and third arc short-circuited.

(a) Substation transformer voltages to earth.
(b) 11 kV phase currents.
(c) Furnace transformer voltages between phases.

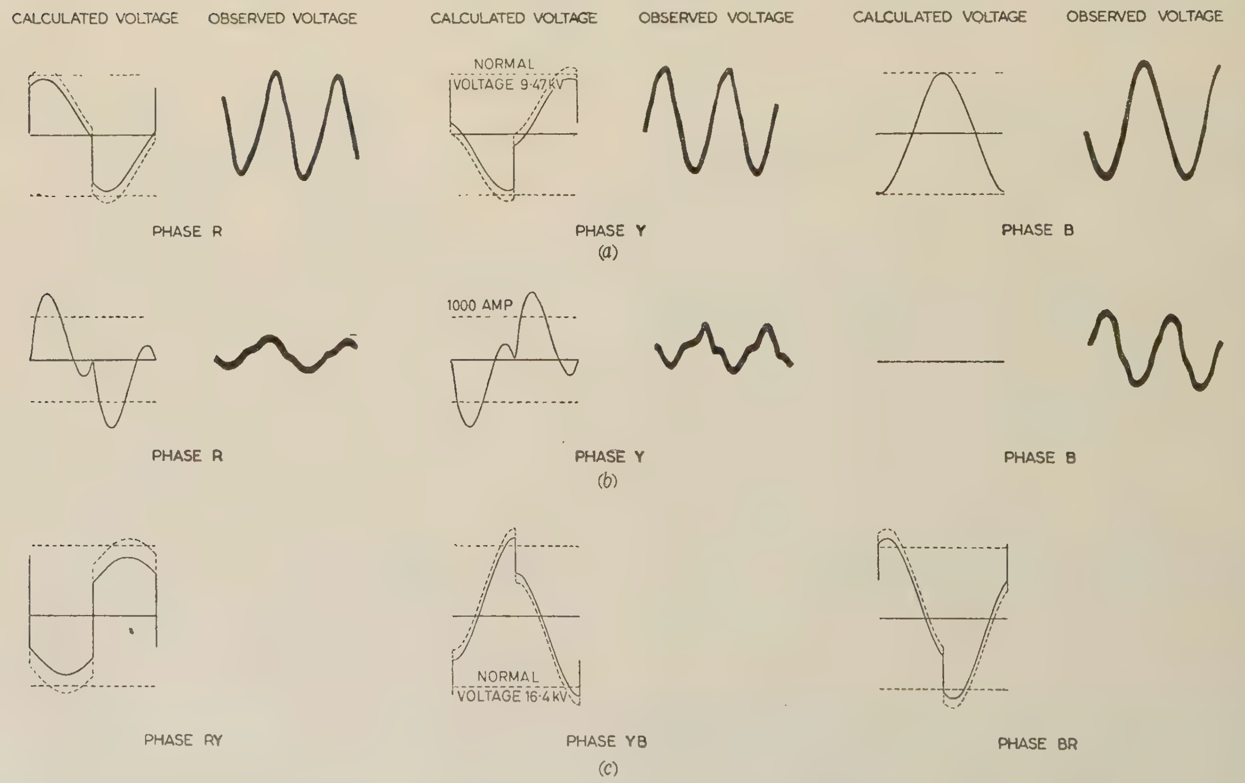

Fig. 15.—Voltages and currents with 2-phase arcing, and third arc, phase B, extinguished.

(a) Substation transformer voltages to earth.
(b) 11 kV phase currents.
(c) Furnace transformer voltages between phases.

0% above the mean value, which was assumed to be 190 volts or the first two conditions and 150 volts for the third, since 90 volts was not theoretically possible.

(4) COMPARISON BETWEEN THEORETICAL AND TEST RESULTS

The oscillograms in Figs. 12–15 show a certain similarity etween the test and theoretical voltage waveforms on the 11 kV etwork. As would be expected the test waveforms are not o clear cut in shape as the theoretical curves owing to the eviation of the arc waveform from the theoretical rectangular hape. If the arc-voltage drops assumed have a peak value 50% bove the mean rectangular value, the theoretical maximum oltages at the substation transformer l.v. terminals are as given n Table 1.

These voltages are somewhat lower than those indicated n Fig. 4, but are comparable with the lower portion of the

Table 1

Condition	Max. voltage	Percentage of normal voltage
	kV	%
3-phase arcing	11·7	124
2-phase arcing—arc short-circuited ..	9·6	101
2-phase arcing—arc extinguished ..	10·5	111

urve. The value of 50% assumed for the peak deviations of he arc waveform from the regular rectangular shape is somewhat arbitrary. The maximum voltage of 13·5 kV, or 143% ormal voltage as measured during the tests, would theoretically equire an arc-voltage drop of about 340 volts, which is quite within the measured range of arc voltage.

The theoretical calculations indicate that serious over-voltages nay also be anticipated on the furnace transformer itself. The naximum stress then occurs with one arc extinguished, when a 0% over-voltage gives 20·8 kV transformer voltage, or 127% f the normal voltage. This is of the same order as those on the ubstation transformer given in Table 1.

(4.1) Supply-Voltage Fluctuations

An estimate of the probable variations which may occur on he substation transformer h.v. winding may be made from a onsideration of the impedances of the network. Using Thévenin's theorem the impedance of the network may be valuated 'looking in' from the substation and including the arious power-station impedances. This was found to be ·83 + $j7{\cdot}2$ ohms. The substation transformer impedance eferred to the h.v. winding was 36·3 ohms. This means that bout 17% of the 11 kV fluctuations occurred on the 66 kV ystem. Assuming fluctuations of 25% line voltage, which is conservative measured maximum value, this means that luctuations of about 4% of the supply voltage occurred on the 6 kV distribution system. The authors found it possible to bserve fluctuations due to the furnace in the lighting supply bout two miles from the furnace. These were not sufficient to ause any annoyance, but they did hear reports of more severe isturbances in the neighbourhood of the furnace.

(5) CONCLUSIONS

An electric arc furnace has long been recognized as a rather ficult load for the supply engineer, since it produces large rapid fluctuations in load current. These are clearly shown in the ammeter records reproduced in Figs. 6–8. These fluctuations in current produce voltage variations on the connected power systems in the neighbourhood unless these have a sufficiently low impedance to carry the load without undue voltage drop.

In the plant tested it is estimated that fluctuations of 3 or 4% of the 66 kV line voltage will occur. This is sufficient to produce noticeable voltage variations on the neighbouring distribution systems. The line short-circuit capacity at the substation was estimated at 500 MVA. Compared with the furnace transformers' capacity of 15 MVA this gives a ratio of 33 : 1. It is less than the minimum capacity ratio of 51 : 1 recommended by Ramsaur and Treweek[9] and shown in Fig. 1. Since the tests were carried out, supply-system extensions in the neighbourhood have enabled the furnace supply to be obtained from a nearby 132/66 kV supply point having a lower impedance.

Over-voltages of nearly 50% above the normal line voltages were measured during the tests. From the theoretical analysis it appears that these are due to the different waveforms produced by the power stations and by the arcs. These voltages will be uniformly distributed throughout the windings, and are connected with the main fluxes of the transformers. It is therefore unnecessary to provide the transformers with reinforced end insulation as is necessary for lightning surges. It is, however, usual to build furnace transformers with a higher level of insulation than is necessary with normal service transformers.

The voltages across the arcs appear to vary over a very wide range, judging from the approximate values obtained from tests. The minimum value obtained was about 110 volts for normal arcing conditions. The maximum value for an arc voltage which resembled a rectangular waveform was about 200 volts. This is also about the maximum theoretical rectangular arc voltage obtainable. Other irregular arc voltages were recorded with peaks up to about 400 volts. Hence, when attempting to evaluate the maximum voltages likely to occur in a particular part of the circuit, it would seem desirable to assume the maximum rectangular voltage possible and then increase the arc-voltage component by, say, 75% over-voltage to allow for these irregular conditions. The curve in Fig. 4 shows that most of the over-voltages on the transformers have a much lower value corresponding to a lower or more regular arc-voltage waveform.

The charts from the recording ammeter show that it is possible to have currents varying from about 1 500 amp to zero in one or two minutes in extreme cases, as in Fig. 8. This is about twice the normal rated current of the transformers. During the initial portion of the melting or 'cogging down' process, current variations of about 500 amp occur very frequently, as in Fig. 6. These may go on continuously for considerable periods. The voltage changes taking place simultaneously with these current fluctuations were clearly visible on a lamp fed from the 11 kV system. Owing to the short duration of these excess currents, they do not involve any thermal overloading of the transformers.

(6) ACKNOWLEDGMENTS

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(8) APPENDIX—ANALYSIS OF ARCING CONDITIONS

The basic circuit for analysis as developed using the assumptions made in Section 3 is given in Fig. 16.

(8.1) General Circuit Relations

Writing down the general equations for the three phases of the 11 kV network as shown in Fig. 16 and using the symbols previously defined:

For phase RY,

$$(V_S)_{RY} = L_1\left(\frac{dI_R}{dt} - \frac{dI_Y}{dt}\right) + L_2\frac{dI_{RY}}{dt} + v_{RY} \quad . \quad . \quad (1)$$

or

$$V_S \sin(\omega t + \phi) = L_1\left(\frac{dI_R}{dt} - \frac{dI_Y}{dt}\right) + L_2\frac{dI_{RY}}{dt} + v_{RY} \quad (2)$$

Similarly, for phase YB,

$$V_S \sin\left(\omega t + \phi + \frac{2\pi}{3}\right) = L_1\left(\frac{dI_Y}{dt} - \frac{dI_B}{dt}\right) + L_2\frac{dI_{YB}}{dt} + v_{YB} \quad (3)$$

and for phase BR,

$$V_S \sin\left(\omega t + \phi + \frac{4\pi}{3}\right) = L_1\left(\frac{dI_B}{dt} - \frac{dI_R}{dt}\right) + L_2\frac{dI_{BR}}{dt} + v_{BR} \quad (4)$$

By Kirchhoff's laws, at the terminals of the furnace transformer,

$$I_R = I_{RY} - I_{BR} \quad . \quad . \quad . \quad . \quad . \quad . \quad (5)$$

$$I_Y = I_{YB} - I_{RY} \quad . \quad . \quad . \quad . \quad . \quad . \quad (6)$$

$$I_B = I_{BR} - L_{YB} \quad . \quad . \quad . \quad . \quad . \quad . \quad (7)$$

Substituting eqns. (5)–(7) in eqn. (2),

$$V_S \sin(\omega t + \phi) = L_1\frac{d}{dt}(I_{RY} - I_{BR} - I_{YB} + I_{RY}) + L_2\frac{dI_{RY}}{dt} + v_{RY} \quad . \quad (8)$$

Since there is no delta circulating current,

$$I_{RY} + I_{YB} + I_{BR} = 0 \quad . \quad . \quad . \quad . \quad . \quad (9)$$

$$V_S \sin(\omega t + \phi) = 3L_1\frac{dI_{RY}}{dt} + L_2\frac{dI_{RY}}{dt} + v_{RY} \quad . \quad (10)$$

$$\frac{dI_{RY}}{dt} = \frac{1}{3L_1 + L_2}[V_S \sin(\omega t + \phi) - v_{RY}] \quad . \quad (11)$$

Similarly,

$$\frac{dI_{BR}}{dt} = \frac{1}{3L_1 + L_2}\left[V_S \sin\left(\omega t + \phi + \frac{4\pi}{3}\right) - v_{BR}\right] \quad (12)$$

Subtracting eqns. (11) and (12) and integrating,

$$I_R = I_{RY} - I_{BR}$$

$$= \frac{1}{3L_1 + L_2}\int\left\{V_S\left[\sin(\omega t + \phi) - \sin\left(\omega t + \phi + \frac{4\pi}{3}\right)\right] - (v_{RY} - v_{BR})\right\}dt \quad (13)$$

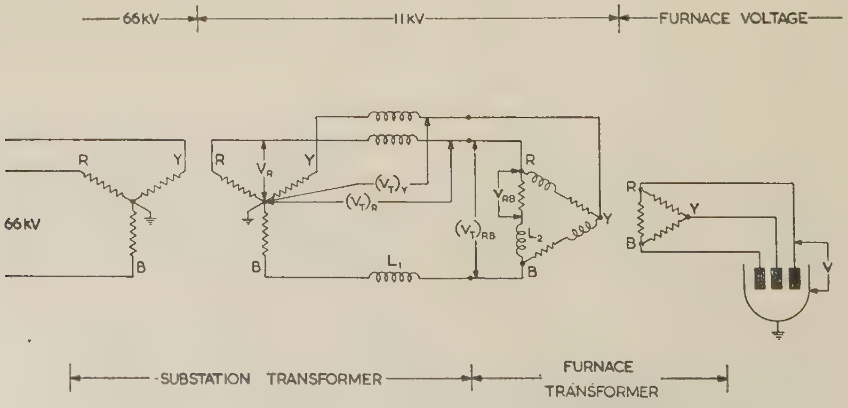

Fig. 16.—Equivalent circuit for supply of arc furnace from 66 kV Grid.

$$= \frac{1}{3L_1+L_2}\int\left[2V_S\cos\left(\omega t+\phi+\frac{2\pi}{3}\right)\sin\left(-\frac{2\pi}{3}\right)\right.$$

$$\left.-(v_{RY}-v_{BR})\right]dt \quad (14)$$

$$= \frac{1}{3L_1+L_2}\int\left[-(\sqrt{3})V_S\cos\left(\omega t+\phi+\frac{2\pi}{3}\right)\right.$$

$$\left.-(v_{RY}-v_{BR})\right]dt \quad (15)$$

Hence the reactance voltage in phase R is

$$(v_R)_R = -L_1\frac{dI_R}{dt} \quad \ldots \quad (16)$$

$$= \frac{L_1}{3L_1+L_2}\left[(\sqrt{3})V_S\cos\left(\omega t+\phi+\frac{2\pi}{3}\right)+(v_{RY}-v_{BR})\right] \quad \ldots \quad (17)$$

The voltage at the substation transformer terminal will be

$$(v_T)_R = (V_S)_R-(v_R)_R \quad \ldots \quad (18)$$

$$= \frac{V_S}{\sqrt{3}}\sin\left(\omega t+\phi-\frac{5\pi}{6}\right)-\frac{L_1}{3L_1+L_2}$$

$$\left[(\sqrt{3})V_S\cos\left(\omega t+\phi+\frac{2\pi}{3}\right)+v_{RY}-v_{BR}\right] \quad (19)$$

$$= \frac{V_S}{\sqrt{3}}\left(1-\frac{3L_1}{3L_1+L_2}\right)\sin\left(\omega t+\phi-\frac{5\pi}{6}\right)$$

$$-\frac{L_1}{3L_1+L_2}(v_{RY}-v_{BR}) \quad (20)$$

$$= \frac{V_S}{\sqrt{3}}\frac{L_2}{3L_1+L_2}\sin\left(\omega t+\phi-\frac{5\pi}{6}\right)$$

$$-\frac{L_1}{3L_1+L_2}(v_{RY}-v_{BR}) \quad (21)$$

Similarly,

$$(v_T)_Y = \frac{V_S}{\sqrt{3}}\frac{L_2}{3L_1+L_2}\sin\left(\omega t+\phi-\frac{\pi}{6}\right)$$

$$-\frac{L_1}{3L_1+L_2}(v_{YB}-v_{RY}) \quad . \quad (22)$$

$$(v_T)_B = \frac{V_S}{\sqrt{3}}\frac{L_2}{3L_1+L_2}\sin\left(\omega t+\phi+\frac{\pi}{2}\right)$$

$$-\frac{L_1}{3L_1+L_2}(v_{BR}-v_{YB}) \quad . \quad (23)$$

Similarly the furnace transformer voltages may be derived from eqns. (11), (12), etc.:

$$(v_T)_{RY} = L_2\frac{dI_{RY}}{dt}+v_{RY} \quad \ldots \quad (24)$$

$$= \frac{L_2}{3L_1+L_2}V_S\sin(\omega t+\phi)+\frac{3L_1}{3L_1+L_2}v_{RY} \quad (25)$$

$$(v_T)_{YB} = \frac{L_2}{3L_1+L_2}V_S\sin\left(\omega t+\phi+\frac{2\pi}{3}\right)+\frac{3L_1}{3L_1+L_2}v_{YB} \quad (26)$$

$$(v_T)_{BR} = \frac{L_2}{3L_1+L_2}V_S\sin\left(\omega t+\phi+\frac{4\pi}{3}\right)+\frac{3L_1}{3L_1+L_2}v_{BR} \quad (27)$$

(8.2) **3-Phase Arcing**

Let the arc voltage in phase R be written as

$$v_{or} = V\text{sq}(\omega t) \quad \ldots \quad (28)$$

and

$$v_{oy} = V\text{sq}\left(\omega t+\frac{2\pi}{3}\right) \quad \ldots \quad (29)$$

$$v_{ob} = V\text{sq}\left(\omega t+\frac{4\pi}{3}\right) \quad \ldots \quad (30)$$

It can be shown that the arc voltages referred to the h.v. side of the furnace transformer may be written as

$$v_{RY} = Vk\left[\text{sq}\left(\omega t+\frac{2\pi}{3}\right)-\text{sq}(\omega t)\right] \quad \ldots \quad (31)$$

$$v_{YB} = Vk\left[\text{sq}\left(\omega t+\frac{4\pi}{3}\right)-\text{sq}\left(\omega t+\frac{2\pi}{3}\right)\right] \quad . \quad (32)$$

$$v_{BR} = Vk\left[\text{sq}(\omega t)-\text{sq}\left(\omega t+\frac{4\pi}{3}\right)\right] \quad \ldots \quad (33)$$

Hence, from eqn. (15),

$$I_R = \frac{1}{3L_1+L_2}\int\left[-V_S(\sqrt{3})\cos\left(\omega t+\phi+\frac{2\pi}{3}\right)\right.$$

$$\left.-(v_{RY}-v_{BR})\right]dt \quad (34)$$

$$= \frac{1}{3L_1+L_2}\left\{-V_S\frac{\sqrt{3}}{\omega}\sin\left(\omega t+\phi+\frac{2\pi}{3}\right)\right.$$

$$\left.-Vk\int\left[\text{sq}\left(\omega t+\frac{2\pi}{3}\right)-2\text{sq}(\omega t)+\text{sq}\left(\omega t+\frac{4\pi}{3}\right)\right]dt\right\} \quad (35)$$

since the arc current in phase R must vanish when the arc voltage passes through zero, i.e. when $\omega t = 0$. From the circuit it can also be shown that $I_R = 0$ when $\omega t = 0$.

$$0 = -V_S\frac{\sqrt{3}}{\omega}\sin\left(\phi+\frac{2\pi}{3}\right)-Vk\frac{\pi}{2\omega}\left(\frac{1}{3}+2+\frac{1}{3}\right) \quad (36)$$

The evaluation of the integrals is given in Reference 5.

$$\sin\left(\omega t+\frac{2\pi}{3}\right) = -\frac{4\pi}{3\sqrt{3}}\frac{Vk}{V_S} \quad \ldots \quad (37)$$

$$\sin\left(\omega t-\frac{\pi}{3}\right) = \frac{4\pi}{3\sqrt{3}}\frac{Vk}{V_S} \quad \ldots \quad (38)$$

Consideration of the possible phase relationships adds the additional condition

$$\frac{5\pi}{6} > \phi > \frac{\pi}{3}$$

From eqn. (21) the substation transformer voltage is given by

$$(v_T)_R = \frac{V_S}{\sqrt{3}}\frac{L_2}{3L_1+L_2}\sin\left(\omega t+\phi-\frac{5\pi}{6}\right)$$

$$-\frac{VkL_1}{3L_1+L_2}\left[\text{sq}\left(\omega t+\frac{2\pi}{3}\right)-2\,\text{sq}(\omega t)+\text{sq}\left(\omega t+\frac{4\pi}{3}\right)\right] \quad (39)$$

And the furnace transformer voltage is given by eqn. (25):

$$(v_T)_{RY} = \frac{L_2}{3L_1+L_2}V_S\sin(\omega t+\phi)+\frac{3L_1}{3L_1+L_2}v_{RY}$$

$$= \frac{L_2}{3L_1 + L_2} V_S \sin(\omega t + \phi) + \frac{3L_1}{3L_1 + L_2} Vk\left[\mathrm{sq}\left(\omega t + \frac{2\pi}{3}\right) - \mathrm{sq}(\omega t)\right] \quad . \quad (40)$$

These voltages are plotted in Fig. 13.

(8.3) Two-Phase Arcing—One Arc Short-Circuited

Assume the arc in phase B is short-circuited. It is then shown in the previous paper[5] that the furnace transformer l.v. winding and arc voltages may be written [see eqns. (6), (7), (8) and (55) of that paper) as

$$v_{br} = v_{or} = V \mathrm{sq}(\omega t) \quad . \quad . \quad . \quad . \quad . \quad (41)$$

$$v_{yb} = -v_{oy} = -V \mathrm{sq}\left(\omega t + \frac{2\pi}{3}\right) \quad . \quad . \quad . \quad (42)$$

$$v_{ry} = V\left[\mathrm{sq}\left(\omega t + \frac{2\pi}{3}\right) - \mathrm{sq}(\omega t)\right] \quad . \quad . \quad (43)$$

Since the arc current and voltage must be in phase, the current in phase r (and, of course, phase R) must pass through zero at $\omega t = 0$. Transferring the voltages from the l.v. to the h.v. winding of the arc furnace transformer, and substituting the values in eqn. (15),

$$I_R = \frac{1}{3L_1 + L_2}\int\left\{-(\sqrt{3})V_S \cos\left(\omega t + \phi + \frac{2\pi}{3}\right) - Vk\left[\mathrm{sq}\left(\omega t + \frac{2\pi}{3}\right) - 2\,\mathrm{sq}(\omega t)\right]dt\right\} \quad (44)$$

$$= \frac{1}{3L_1 + L_2}\left\{\frac{-(\sqrt{3})V_S}{\omega}\sin\left(\omega t + \phi + \frac{2\pi}{3}\right) - Vk\int\left[\mathrm{sq}\left(\omega t + \frac{2\pi}{3}\right) - 2\,\mathrm{sq}(\omega t)\right]dt\right\} \quad (45)$$

At $\omega t = 0$,

$$0 = -\frac{\sqrt{3}}{\omega}V_S \sin\left(\phi + \frac{2\pi}{3}\right) - Vk\frac{\pi}{2\omega}\left(\frac{1}{3} + 2\right) \quad (46)$$

Therefore

$$(\sqrt{3})V_S \sin\left(\phi - \frac{\pi}{3}\right) = Vk\frac{\pi}{2}\frac{7}{3} \quad . \quad . \quad . \quad (47)$$

$$\sin\left(\phi - \frac{\pi}{3}\right) = \frac{7\pi}{6\sqrt{3}}\frac{Vk}{V_S} \quad . \quad . \quad . \quad (48)$$

Consideration of the various phase relations give the additional limitation

$$\frac{\pi}{3} < \phi < \frac{5\pi}{6} \text{ (approximately)}$$

Substituting values in eqns. (21)–(23) to obtain the substation transformer voltages,

$$(v_T)_R = \frac{V_S}{\sqrt{3}}\frac{L_2}{3L_1 + L_2}\sin\left(\omega t + \phi - \frac{5\pi}{6}\right) - \frac{L_1 Vk}{3L_1 + L_2}\left[\mathrm{sq}\left(\omega t + \frac{2\pi}{3}\right) - 2\,\mathrm{sq}(\omega t)\right] \quad (49)$$

$$(v_T)_Y = \frac{V_S}{\sqrt{3}}\frac{L_2}{3L_1 + L_2}\sin\left(\omega t + \phi - \frac{\pi}{6}\right) + \frac{L_1 Vk}{3L_1 + L_2}\left[2\,\mathrm{sq}\left(\omega t + \frac{2\pi}{3}\right) - \mathrm{sq}(\omega t)\right] \quad (50)$$

$$(v_T)_B = \frac{V_S}{\sqrt{3}}\frac{L_2}{3L_1 + L_2}\sin\left(\omega t + \phi + \frac{\pi}{2}\right) - \frac{L_1 Vk}{3L_1 + L_2}\left[\mathrm{sq}(\omega t) + \mathrm{sq}\left(\omega t + \frac{2\pi}{3}\right)\right] \quad (51)$$

The furnace transformer voltages may be obtained from eqns. (25) to (27) as follows:

$$(v_T)_{RY} = \frac{L_2 V_S}{3L_1 + L_2}\sin(\omega t + \phi) + \frac{3L_1 Vk}{3L_1 + L_2}\left[\mathrm{sq}\left(\omega t + \frac{2\pi}{3}\right) - \mathrm{sq}(\omega t)\right] \quad (52)$$

$$(v_T)_{YB} = \frac{L_2 V_S}{3L_1 + L_2}\sin\left(\omega t + \phi + \frac{2\pi}{3}\right) - \frac{3L_1 Vk}{3L_1 + L_2}\mathrm{sq}\left(\omega t + \frac{2\pi}{3}\right) \quad (53)$$

$$(v_T)_{BR} = \frac{L_2 V_S}{3L_1 + L_2}\sin\left(\omega t + \phi + \frac{4\pi}{3}\right) + \frac{3L_1 Vk}{3L_1 + L_2}\mathrm{sq}(\omega t) \quad (54)$$

These voltages are plotted in Fig. 14.

From eqn. (15) and the various phase relations,

$$I_Y = \frac{1}{3L_1 + L_2}\int\left[-(\sqrt{3})V_S \cos\left(\omega t + \phi + \frac{4\pi}{3}\right) - (v_{YB} - v_{RY})\right]dt \quad (55)$$

$$= \frac{1}{3L_1 + L_2}\left[-\frac{(\sqrt{3})V_S}{\omega}\sin\left(\omega t + \phi + \frac{4\pi}{3}\right) - \int(v_{YB} - v_{RY})dt\right] \quad (56)$$

Similarly,

$$I_B = \frac{1}{3L_1 + L_2}\left[-\frac{(\sqrt{3})V_S}{\omega}\sin(\omega t + \phi) - \int(v_{BR} - v_{YB})dt\right] \quad (57)$$

Substituting values for v_{RY}, v_{YB} and v_{BR},

$$I_Y = \frac{1}{3L_1 + L_2}\left\{-\frac{(\sqrt{3})V_S}{\omega}\sin\left(\omega t + \phi + \frac{4\pi}{3}\right) - Vk\int\left[-2\,\mathrm{sq}\left(\omega t + \frac{2\pi}{3}\right) + \mathrm{sq}(\omega t)\right]dt\right\} \quad (58)$$

$$I_B = \frac{1}{3L_1 + L_2}\left\{-\frac{(\sqrt{3})V_S}{\omega}\sin(\omega t + \phi) - Vk\int\left[\mathrm{sq}(\omega t) + \mathrm{sq}\left(\omega t + \frac{2\pi}{3}\right)\right]dt\right\} \quad (59)$$

(8.4) Two-Phase Arcing with One Arc Extinguished

Assume the arc in phase B is extinguished.

From eqn. (11) in the previous paper,[5] the voltage across the transformer l.v. phase RY is given by

$$v_{ry} = -2V \mathrm{sq}(\omega t) \quad . \quad . \quad . \quad . \quad . \quad (60)$$

Since there is no arc in phase B the current $I_B = 0$. Hence, from eqn. (15),

$$I_B = \frac{1}{3L_1 + L_2}\int[-(\sqrt{3})V_S \cos(\omega t + \phi) - (v_{BR} - v_{YB})]dt = 0 \quad . \quad . \quad . \quad . \quad (61)$$

Therefore $$v_{BR} - v_{YB} = -(\sqrt{3})V_S \cos(\omega t + \phi) \quad . \quad . \quad (62)$$

but $$v_{BR} + v_{YB} = -v_{RY} = 2Vk \operatorname{sq}(\omega t) \quad . \quad . \quad (63)$$

$$v_{BR} = Vk \operatorname{sq}(\omega t) - \frac{\sqrt{3}}{2} V_S \cos(\omega t + \phi) \quad . \quad . \quad (64)$$

$$v_{YB} = Vk \operatorname{sq}(\omega t) + \frac{\sqrt{3}}{2} V_S \cos(\omega t + \phi) \quad . \quad . \quad (65)$$

Now I_R must be in phase with v_{RY} since the arc voltage and current must have the same zero passages, i.e. $I_R = 0$ when $\omega t = 0$. From eqn. (15),

$$I_R = \frac{1}{3L_1 + L_2} \int \left\{ -(\sqrt{3}) Y_S \cos\left(\omega t + \phi + \frac{2\pi}{3}\right) - \left[-2Vk \operatorname{sq}(\omega t) - Vk \operatorname{sq}(\omega t) + \frac{\sqrt{3}}{2} V_S \cos(\omega t + \phi) \right] \right\} dt \quad (66)$$

$$= \frac{1}{3L_1 + L_2} \left[\frac{-\sqrt{3}}{\omega} V_S \sin\left(\omega t + \phi + \frac{2\pi}{3}\right) + \int 3Vk \operatorname{sq}(\omega t) dt - \frac{\sqrt{3}}{2} \frac{V_S}{\omega} \sin(\omega t + \phi) \right] \quad (67)$$

Taking the condition that $\omega t = 0$,

$$-\frac{\sqrt{3}}{\omega} V_S \sin\left(\phi + \frac{2\pi}{3}\right) - \frac{3\pi}{2\omega} Vk - \frac{\sqrt{3}}{2\omega} V_S \sin\phi = 0 \quad (68)$$

Therefore

$$(\sqrt{3}) V_S \left[\sin\left(\phi + \frac{2\pi}{3}\right) + \frac{1}{2} \sin\phi \right] = -\frac{3\pi}{2} Vk \quad (69)$$

and

$$V_S \left[\sin\phi\left(-\frac{1}{2}\right) + \frac{\sqrt{3}}{2} \cos\phi + \frac{1}{2} \sin\phi \right] = -\frac{(\sqrt{3})\pi}{2} Vk \quad (70)$$

$$\cos\phi = -\frac{\pi Vk}{V_S} \quad . \quad . \quad . \quad . \quad . \quad (71)$$

Consideration of possible solutions gives the additional limitation

$$\frac{\pi}{2} < \phi < \pi \quad . \quad . \quad . \quad . \quad . \quad . \quad (72)$$

Substituting eqns. (60), (64) and (65) in eqns. (21)–(23) to obtain the substation transformer voltages,

$$(v_T)_R = \frac{V_S}{\sqrt{3}} \frac{L_2}{3L_1 + L_2} \sin\left(\omega t + \phi - \frac{5\pi}{6}\right) - \frac{L_1}{3L_1 + L_2} \left[-2Vk \operatorname{sq}(\omega t) - Vk \operatorname{sq}(\omega t) + \frac{\sqrt{3}}{2} V_S \cos(\omega t + \phi) \right] \quad (73)$$

$$= \frac{V_S}{\sqrt{3}} \left[\frac{-L_2}{3L_1 + L_2} \sin\left(\omega t + \phi + \frac{\pi}{6}\right) - \frac{L_1}{3L_1 + L_2} \frac{3}{2} \cos(\omega t + \phi) \right] + \frac{3L_1}{3L_1 + L_2} Vk \operatorname{sq}(\omega t) \quad (74)$$

$$= \frac{V_S}{\sqrt{3}} \left\{ -\frac{L_2}{3L_1 + L_2} \left[\frac{\sqrt{3}}{2} \sin(\omega t + \phi) + \frac{1}{2} \cos(\omega t + \phi) \right] - \frac{3L_1}{3L_1 + L_2} \frac{1}{2} \cos(\omega t + \phi) \right\} + \frac{3L_1}{3L_1 + L_2} Vk \operatorname{sq}(\omega t) \quad . \quad . \quad . \quad . \quad (75)$$

$$= \frac{V_S}{\sqrt{3}} \left[-\frac{\sqrt{3}}{2} \frac{L_2}{3L_1 + L_2} \sin(\omega t + \phi) - \frac{1}{2} \cos(\omega t + \phi) \right] + \frac{3L_1}{3L_1 + L_2} Vk \operatorname{sq}(\omega t) \quad (76)$$

$$(v_T)_Y = \frac{V_S}{\sqrt{3}} \frac{L_2}{3L_1 + L_2} \sin\left(\omega t + \phi - \frac{\pi}{6}\right) - \frac{L_1}{3L_1 + L_2} \times \left[3Vk \operatorname{sq}(\omega t) + \frac{\sqrt{3}}{2} \cos(\omega t + \phi) \right] \quad (77)$$

$$= \frac{V_S}{\sqrt{3}} \frac{L_2}{3L_1 + L_2} \left[\frac{\sqrt{3}}{2} \sin(\omega t + \phi) - \frac{1}{2} \cos(\omega t + \phi) \right] - \frac{3L_1 V_S}{\sqrt{3}(3L_1 + L_2)} \frac{1}{2} \cos(\omega t + \phi) - \frac{3L_1}{3L_1 + L_2} Vk \operatorname{sq}(\omega t) \quad (78)$$

$$= \frac{V_S}{\sqrt{3}} \left[\frac{\sqrt{3}}{2} \frac{L_2}{3L_1 + L_2} \sin(\omega t + \phi) - \frac{1}{2} \cos(\omega t + \phi) \right] - \frac{3L_1}{3L_1 + L_2} Vk \operatorname{sq}(\omega t) \quad (79)$$

$$(v_T)_B = \frac{V_S}{\sqrt{3}} \frac{L_2}{3L_1 + L_2} \sin\left(\omega t + \phi + \frac{\pi}{2}\right) + \frac{L_1 V_S}{3L_1 + L_2} (\sqrt{3}) \cos(\omega t + \phi) \quad (80)$$

$$= \frac{V_S}{\sqrt{3}} \cos(\omega t + \phi) \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (81)$$

Similarly the three furnace transformer voltages may be obtained by substituting eqns. (60), (64) and (65) in eqns. (25)–(27):

$$(v_T)_{RY} = \frac{L_2}{3L_1 + L_2} V_S \sin(\omega t + \phi) - \frac{6L_1}{3L_1 + L_2} Vk \operatorname{sq}(\omega t) \quad (82)$$

$$(v_T)_{YB} = \frac{L_2}{3L_1 + L_2} V_S \sin\left(\omega t + \phi + \frac{2\pi}{3}\right) + \frac{3L_1}{3L_1 + L_2} \left[Vk \operatorname{sq}(\omega t) + \frac{\sqrt{3}}{2} V_S \cos(\omega t + \phi) \right] \quad (83)$$

$$= \frac{L_2}{3L_1 + L_2} V_S \left[-\frac{1}{2} \sin(\omega t + \phi) + \frac{\sqrt{3}}{2} \cos(\omega t + \phi) \right] + \frac{3L_1 V_S}{3L_1 + L_2} \frac{\sqrt{3}}{2} \cos(\omega t + \phi) + \frac{3L_1}{3L_1 + L_2} Vk \operatorname{sq}(\omega t) \quad . \quad . \quad . \quad . \quad (84)$$

$$= V_S \left[\frac{\sqrt{3}}{2} \cos(\omega t + \phi) - \frac{1}{2} \frac{L_2}{3L_1 + L_2} \sin(\omega t + \phi) \right] + \frac{3L_1 Vk}{3L_1 + L_2} \operatorname{sq}(\omega t) \quad (85)$$

$$(v_T)_{BR} = \frac{L_2}{3L_1 + L_2} V_S \sin\left(\omega t + \phi + \frac{4\pi}{3}\right) + \frac{3L_1}{3L_1 + L_2} \times \left[Vk \operatorname{sq}(\omega t) - \frac{\sqrt{3}}{2} V_S \cos(\omega t + \phi) \right] \quad (86)$$

$$= V_S\left\{\frac{L_2}{3L_1+L_2}\left[-\frac{1}{2}\sin(\omega t+\phi)-\frac{\sqrt{3}}{2}\cos(\omega t+\phi)\right]\right.$$
$$\left.-\frac{\sqrt{3}}{2}\,\frac{3L_1}{3L_1+L_2}\cos(\omega t+\phi)\right\}$$
$$+\frac{3L_1}{3L_1+L_2}Vk\,\mathrm{sq}\,(\omega t) \quad (87)$$

$$= V_S\left[-\frac{L_2}{3L_1+L_2}\,\tfrac{1}{2}\sin(\omega t+\phi)-\frac{\sqrt{3}}{2}\cos(\omega t+\phi)\right]$$
$$+\frac{3L_1}{3L_1+L_2}Vk\,\mathrm{sq}\,(\omega t) \quad (88)$$

These results are plotted in Fig. 15.

DISCUSSION ON THE ABOVE PAPER

Before the Joint Meeting of the UTILIZATION *and* SUPPLY SECTIONS *13th February, the* NORTH-EASTERN CENTRE *at* NEWCASTLE UPON TYNE *10th February, and the* SHEFFIELD SUB-CENTRE *at* SHEFFIELD *19th February,* 1958.

Mr. F. Seddon: I will confine my remarks to the effects of large arc-furnace loads on the supply system, and to examination of the problems that these highly fluctuating loads create for the supply engineer.

Until the 60-ton 15 MVA arc furnace described in the paper was installed in 1954, the biggest one in this country was, I believe, of 7·5 MVA rating. Arc furnaces were used exclusively for making high-alloy-content steel.

Following the last war, the United States developed arc furnaces with ratings up to 25–30 MVA. The increase in capacity

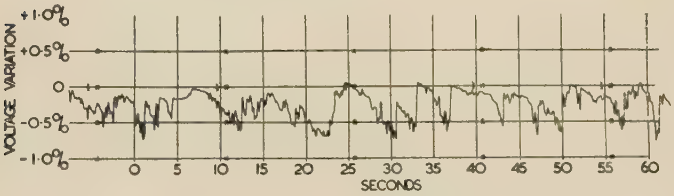

Fig. A

resulted in increased efficiency to such an extent that the arc furnace became economically competitive with the open-hearth process for the making of low-alloy-content steels. The Battelle Report on this experience stimulated similar development here, and a number of large units of capacities from 15 to 36 MVA are now installed or planned, and a 45 MVA furnace is under consideration. The limitation of furnace size appears to be the maximum size of carbon electrode that can be produced. Some of these furnaces are being installed as replacements for open-hearth furnaces. If this trend continues we have the possibility in the Sheffield area alone of arc-furnace loads totalling probably 750–1 000 MVA.

To supply these loads without their causing unacceptable fluctuations in the voltages supplied to other consumers presents the problem indicated by the authors' tests. It is unfortunate that the tests were conducted when the furnace was supplied temporarily from an extended 66 kV system. The same furnace is now supplied from a nearby 132/66 kV substation having a fault level of 850 MVA, and Fig. A shows a chart record of the 66 kV voltage fluctuations now experienced. During the breakdown period these are about 0·5–0·7%. I do not find that they cause irritation and no complaints have been received from consumers. The record was obtained from a 16 in/min comparator-recording voltmeter, the circuit for which is shown in Fig. B. Briefly, the a.c. supply whose voltage variations are to be measured is converted to direct current and compared with a stabilized supply. The difference between the two voltage sources is fed to a servo-operated recorder calibrated to read percentage differences.

Examination of high-speed voltage records shows that during breakdown periods the frequency of the voltage fluctuations of large arc furnaces varies between 10 and 120 per minute. The generally accepted percentage flickers at these frequencies, which are regarded as the borderline of irritation, are 1·2 and 0·5%, respectively. In practice, I have found that fluctuations at the higher frequency of about 0·7% are at the limit of acceptance.

To supply these loads without causing irritation to other consumers requires the furnaces to be supplied from systems having low-impedance sources, and the segregation of such loads from other consumers' supplies so that the common point of connection is at a fault level sufficiently high to absorb the load fluctuations. The authors indicate that Ramsaur and Treweek[9] consider that the system 3-phase short-circuit fault levels should be at least 40–100 times the rated capacity of the furnace transformer. This approximation has been found to be an excellent guide.

No record of any experience in this country in the operation of more than one very large arc furnace on the same system is available, but tests are now in progress to determine the voltage and load fluctuations arising from the simultaneous operation of a 15 and a 20 MVA furnace. It will be interesting to see

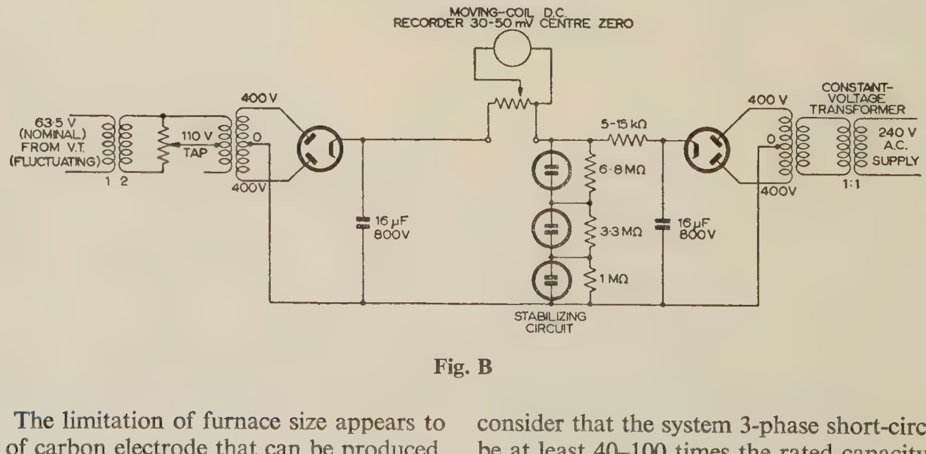

Fig. B

whether these results confirm the American formula that the short-circuit capacity of a system required to supply a number of furnaces varies as the fourth root of the number of furnaces.

A scheme is now being designed to supply two 36 MVA furnaces from the 275 kV Grid system with a 275/33 kV substation on the consumer's premises. The 33 kV voltage fluctuations will be such that the rest of the consumer's load will have to be segregated from the furnace supply transformers, and to reduce the 275 kV system voltage fluctuations to an acceptable value may require the installation of synchronous condensers on the 33 kV supply to the furnaces. Have the authors any experience of the use of synchronous condensers for this purpose, or the use of series static condensers connected in the synchronous condenser output in order to increase the effective capacity?

Mr. E. May: The large arc furnace is, presumably, a remunerative load. The annual electricity bill for one furnace of this size will be about £200 000–£250 000 per annum. The load factor averaged for the whole year, 24 hours per day, is about 37%, the power factor is about 0·8, and the furnace produces about 65 000 tons of steel ingots per annum.

The flicker voltage was measured while the furnace was still operating on a temporary (high-impedance) supply. Since then, the system has been stiffened, and the fault apparent power almost exactly doubled. In addition, a second furnace of 33% higher rating, namely 20 MVA, has been installed, the transformer for this furnace taking its supply directly at 66 kV.

It is important to realize that the author's results are not representative of good practice, because of the relatively low capacity of the supply system at that time. If further tests under present-day conditions are made, I suggest that they should cover the cases of each furnace working separately and also both working together.

The furnace makers have modified the electrode control system in a minor but important detail. Much of the total reactance of such a large furnace and its associated transformer resides in the low-voltage heavy-current furnace circuit itself, and especially in the long flexible cables between the transformer secondary windings and the furnace electrodes.

It is therefore difficult to measure accurately the voltage across the arc. The only convenient point at which to make an arc-voltage connection is at the transformer end of the flexible cables, and, of course, a voltage measured here includes the reactive voltage of the cables, etc., as well as the arc voltage. The difficulty was overcome by causing a voltage to be induced in the measuring circuit equal to, but opposing, that due to circuit reactance (see Fig. C). This modification produced a considerable improvement in electrode control, and I should like to know whether the authors' tests were carried out before or after it was introduced.

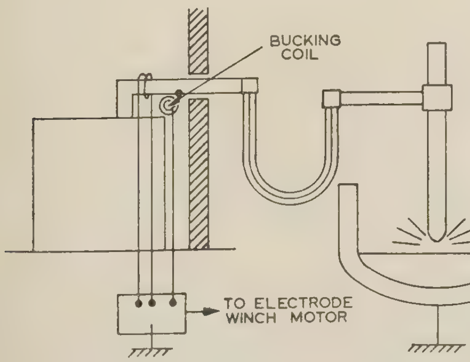

Fig. C

Incidentally, the same principle could be employed to obtain more accurate oscillograms of arc-voltage waveform.

It is almost certainly true that flicker voltages which occur at the rate of several pulses per second cannot be eliminated by any form of mechanical position control of electrodes. Research in progress here and in America on the behaviour of the arc itself may, in time, bring about a solution. Meanwhile, power-factor-correction capacitors will very considerably attenuate the flicker effect produced by surges which last for only $\frac{1}{2}$–1 cycle, and the authors state that most surges are in this category. I understand that, when a synchronous condenser was installed in association with a large furnace in Belgium recently, the supply undertaking contributed towards the cost.

Certain precautions may be necessary when installing power-factor-correction capacitors at two or more arc furnaces in close proximity on the same supply (Fig. D).

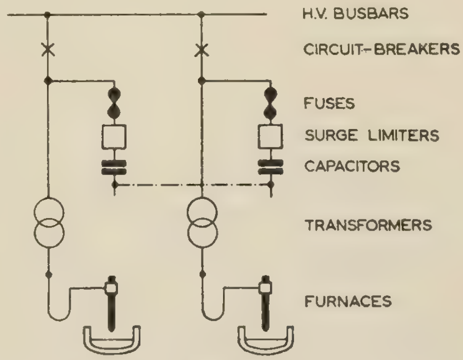

Fig. D

Charge sharing between capacitor banks may be accompanied by large transient h.f. currents, with the possibility of damage to equipment. Cases have occurred where the h.b.c. fuses associated with the capacitors have exploded violently. Surge-limiting reactors are now often fitted, which, while offering a negligible impedance to the normal mains-supply-frequency capacitor current, are designed to absorb the energy of the h.f. transients. They are, in fact, designed as ideal h.f. induction-heated loads.

Mr. R. S. Ryburn: I shall confine my remarks to the questions of what level of voltage fluctuation is tolerable and what steps can be taken to mitigate objectionable interference with other lighting and television supplies. These problems are, unfortunately, not referred to in the paper.

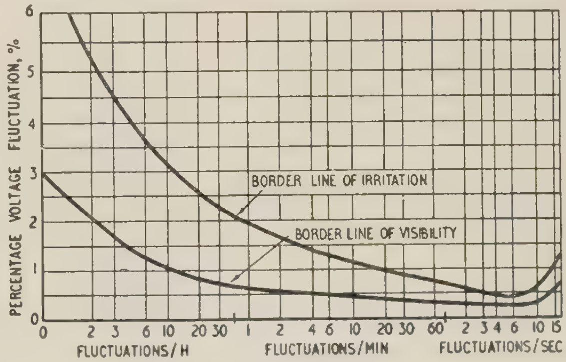

Fig. E

Dealing first with the tolerance question, Fig. E purports to show the border-line of visibility and the border-line of irritation in relation to the frequency of the fluctuations. The frequencies

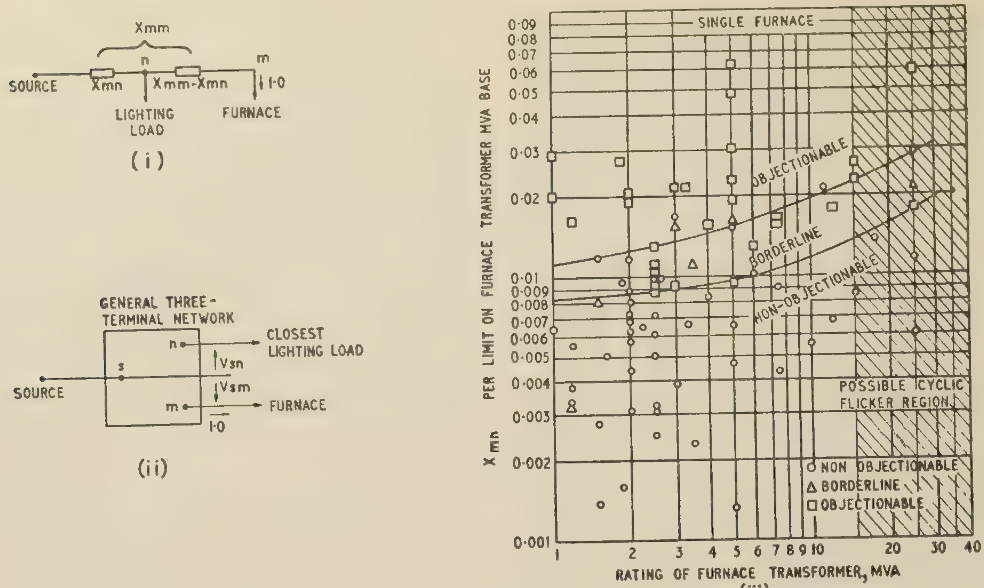

Fig. F

of voltage fluctuations experienced in arc furnaces vary considerably from about one per minute to one per second. This curve is an academic approach and makes no attempt to relate the border-line of irritation to furnace size or furnace characteristics.

American practice, on the other hand, is illustrated in Fig. F, in which the 'mutual reactance' from the source to the point of common connection per unit on the arc-furnace rating is plotted against arc-furnace rating, and it shows the objectionable zone, the border-line cases and the non-objectionable zone.

With regard to the repercussion of excessive voltage fluctuations on the supply systems, the relation quoted between the fault level and the furnace rating can be used as a rough guide only and should be used with care, since, where the whole of the furnace load (active and reactive) is taken from the supply system, regard must be had to the minimum fault level due to changing plant conditions on the system as a whole. Since the furnace supply must be available, on the average, for some 8 000 hours out of the year, minimum plant conditions must be considered. Thus the governing factor is the maximum reactance from the point of common connection back to the source. It would therefore appear that, as the furnace size increases, this point should be made on a higher-voltage network.

Although raising the voltage of the common point increases the capital cost of the furnace supply, it compares favourably with the installation of rotating plant to provide the reactive power. Furthermore there is a conflict in providing, by means of synchronous condensers, a shunt path of sufficiently low reactance to the intermediate busbar in order to make the provision of such plant fully effective in sharing the reactive power with the supply transformers.

Ideally the point of injection of the synchronous plant should be as close to the furnace as possible, and studies at present are being actively pursued into the effect of connecting a synchronous condenser to the tertiary winding on the arc-furnace transformer. Here again questions of stability enter into the problem and require careful co-ordination of plant design.

Mr. T. B. Rolls: That the current and voltage fluctuations will not harm the furnace transformers is an unsatisfactory conclusion. Much study must be given to the pros and cons of shunt and series capacitors, synchronous condensers and buffer reactors, together with various combinations of these, in order to enable furnace ratings to be increased without corresponding increases in the strength of the supply.

On the instrumentation side, the authors seem to have had some difficulty in obtaining good cycle-by-cycle records of voltage and current. A Swedish instrument is now available which will write directly on a paper chart a good cycle-by-cycle record. Thus, if lengthy records are required, there will only be the cost of the paper instead of the film.

Mr. E. L. White: Measurements made by the E.R.A. on a 30 MW calcium-carbide furnace showed that over-voltages of at least 20 times the normal peak voltage occurring between the low-voltage busbars and earth were caused by switching surges on the supply system. The probability of similar over-voltages occurring in the case of a steel furnace should not be overlooked when considering the necessary insulation of the furnace-transformer windings. The fact that the oscillographic equipment described in the paper was not capable of recording short-duration transients does not justify the authors' disregard of over-voltages due to switching surges.

Have the authors investigated arc-voltage waveforms, such as that of Fig. 9(*a*), with a high-speed oscillograph for the presence of short-duration voltage peaks?

By discarding zero-phase-sequence components of the arc voltages and lumping together the various reactances, the equivalent circuit of Fig. 16 can be reduced to a sinusoidal source feeding a non-sinusoidal voltage to the load through a single series reactance. Since the voltage at any intermediate point of the system can then be determined by simple proportion, much of the mathematics of Sections 8.1 and 8.2 is unnecessary. Sections 8.3 and 8.4 are of doubtful value, since it is assumed that the two arc voltages are equal whereas this may not be so in practice.

The authors describe the arc load as having a negative impedance. Apart from the inappropriate use of the word 'impedance', it would be difficult to infer even negative resistance on the evidence of Fig. 9.

It is claimed that stable arcing is connected with thermionic emission from the molten charge. Have the authors confirmed this by observations on the arcs? Since Fe_2O_3 at 2 000° K emits only about 10^{-5} amp/cm^2, does the thermionic current arise from molten slag on the surface of the charge?

Mr. W. G. Hawley: In an electrical sense, the paper helps to

disclose some of the inner secrets of heavy electric-arc furnaces. The authors do not exaggerate when they draw attention to the difficulty such apparatus presents to the supply engineer.

The load in this case is so large that, if it is connected via transformers and an interconnecting feeder to a semi-infinite source of power supply, the voltage drop will be considerable because of the inductive reactance encountered, particularly if the load power factor is unfavourable.

If the arc-furnace installation belongs to a sole user on the supply feeder, the fact that the usual lower statutory limit of voltage variation is exceeded might be of little consequence, especially in this case, where furnace voltage can be adjusted by the means provided. Some alleviation on this score has since been achieved by supplying the furnace from a source of greater power capacity. The lowered voltage might, all the same, cause some trouble in electric motor operation.

But apart from this, consideration ought also to be given to rapid voltage fluctuation caused by furnace operation, which the authors show to be insistent during the melting stages. Light flicker is possibly unbearable, and according to the authors can be experienced several miles away.

It seems that this is an instance where advantage could readily be taken of the self-regulating properties of a series capacitor. The use of capacitors in bringing about efficient transmission of electrical power is becoming well known, and the theory of the series capacitor is now widely understood. The authors would perform a greater service if they could provide information regarding power-factor characteristics during melting and refining periods, and I would like to persuade them to adopt the practice of giving the furnace rating in electrical units.

Mr. J. W. S. Payne: The results obtained by the authors fall into line with the conclusions of the recently published report* of the American I.E.E., in that the furnace on the temporary system described would lead to light flicker. Did the authors measure the flicker quantitatively?

How were the magnitude and number of surges in the oscillograms like Figs. 3 and 5 measured? Surges are clearly affected by the efficiency of the electrode regulator, which, for large furnaces, must have a short dead time, fast speed of response, high top speed for clearing 'cave-ins' and no overshoot. For fast operation under short-circuit conditions it is essential that the regulator be provided with compensation for the reactive voltage drop occurring in the flexible leads from the transformer to the electrodes.

From Fig. 12 it appears that the action of the regulator is rapid, the response starting within a few milliseconds and complete recovery to normal voltage being about 0·3 sec.

The authors state that, owing to a previous over-current surge, the electrode control-gear may withdraw the electrode too far, causing the arc to be extinguished. These initial fluctuations may take place several times a minute. However, from the record of the breakdown period in Fig. 6, the arc current is shown to be maintained throughout, and in Figs. 7 and 8 it would appear that, on the two occasions when the arcs were extinguished, the cause would be due to falling scrap, as the current was relatively stable before each disturbance.

It is impossible for any electrode regulator to correct either for the fluctuations when scrap suddenly falls away or for the short cyclic disturbances described by W. E. Schwabe.†

Figs. 11 and 12 appear to bear out Schwabe's conclusion that these cyclic disturbances occur during the first 15–20 min of the melt. Have the authors found that the current does vary in the cyclic manner at the commencement of a melt?

* 'Arc Furnace Installations and Lamp Flicker', American I.E.E. Sub-Committee Report, *Transactions of the American I.E.E.*, Part II, September, 1957.

† SCHWABE, W. E.: 'Lighting Flicker caused by Electric Arc Furnaces', Paper presented at the 1957 American Iron and Steel Engineers' Convention.

Mr. P. F. Grove: I agree that the paper is timely, because furnaces are being put on all kinds of systems; a 5-ton furnace may be as troublesome as a 60-ton one. Fifteen years ago I was instrumental in installing the first two 33 kV furnace installations in this country. This was somewhat of an adventure because furnace transformers before then had not had conservators. The first was in an iron and steelworks with a buffer reactor connected onto the Grid system 33 kV busbars with 132 kV outside and some shunt capacitors for reducing oil carbonizing in the circuit-breakers. The complaints about flicker were not due to the furnace but to starting a 4 500 h.p. synchronous motor. The second was in the Midlands, and the supply engineer, after being alarmed at the prospect of a 6 MVA arc furnace being connected to the 33 kV system, surprised me by saying that the surges from furnace loads in Sheffield were no worse than those on his industrial 33 kV network. Later in Scotland on an 11 kV system we had a flashover on a 5-ton furnace air-blast circuit-breaker, and tests proved the surges to be coming from outside. On the tests in Wales referred to by Mr. White, I understood the conclusion was that the surges came from a large rectifier installation 20 miles away.

These instances are given only to show that surges do come from outside and that the higher the system voltage used the less trouble there may be on lighting networks. I feel that the 15 MVA transformer described could have been wound for 66 kV, which would conform better to Ramsaur and Treweek's recommendation.

The Americans stressed selection of the best voltage to suit the charge during melting down, and also fast electrode control, both to avoid flicker. The excessive boring down into the charge described might be improved by closer selection of scrap and melting-down voltage.

Mr. M. Waters: Fig. E is a chart for gas-filled lamps taken from E.R.A. Report Ref. K/T110 relating visibility and irritation to percentage voltage fluctuation and frequency. It was obtained from American sources in 1945, and since then doubt has arisen as to the validity of the curves at the lower frequencies. The E.R.A. are preparing a résumé of more recent work on flicker.

One method of avoiding ill effects of current variations not yet mentioned is the use of a separate power station or separate generators specifically for energizing arc furnaces.

Surely it is possible to design a furnace so that the power input is steady during the melting-down process. After all, screwing down three large carbon electrodes on to a pile of scrap steel may be an effective way of melting it, but it is certainly very crude, and it is not surprising that violent current fluctuations are produced. Synchronous condensers, series capacitors, line reinforcement, etc., are expensive. Would not the money spent on them be better used in providing an arc furnace designed, together with its circuit, to be free from the current fluctuations at present associated with the melting-down process?

Mr. J. R. Spanswick: A previous speaker suggested that designers should produce a furnace which does not give rise to surges. Since it is necessary to strike the arc, surges must arise unless a prohibitive reactance is included in the circuit. Furthermore, a furnace has to deal with scrap of varying composition, and, when putting, for example, 12 000 kW into a small space, it is almost impossible to avoid occasional short-circuits to the charge.

It has been suggested that we shall be considering furnaces with ratings of up to 40 or 50 MVA. Furnaces approaching this rating have been installed in the United States, but it is interesting to note an installation made some time ago where the furnace had six electrodes, two groups of three, each connected to an independent transformer. A 3-electrode furnace of comparable total rating is installed in the same works, and the older, six-electrode furnace produces steel more efficiently.

I suggest that requirements for very large furnaces may be better met by the 6-electrode furnace.

With regard to series capacitors, I think we should be cautious, bearing in mind that we have to maintain a certain reactance in the system. Under transient conditions series capacitors may have adverse effects from the point of view of surges.

It is important to distinguish between fluctuations of voltage and surges. The paper deals with voltage fluctuations, but there is some evidence that surges of much higher magnitude do occur.

Finally, reference has been made to comparatively high-frequency fluctuations during the operation of the furnace. A recent American article* related these to cyclic variation of the arc associated with the comparatively large diameter of the electrode. In my experience they do not arise in small furnaces.

Mr. R. G. Round: I would like to comment on a technique for measuring fluctuations. Two or three years ago I was concerned with the problem of supplying three small furnaces, each of $3\frac{1}{2}$ MVA rating. These furnaces had hitherto been supplied through a synchronous motor alternator, and it was proposed to connect them directly to the supply system. We were concerned with the effect that this would have on the system. The problem was approached with the idea that the principal nuisance would be lighting flicker to our domestic consumers, and we wanted to devise a technique of measuring it. We therefore took a filament lamp and put it in a box with a photocell, amplified the output of the photocell and fed it to an oscillograph. The instrument was calibrated by applying known voltage variations to it. We calculated what voltage fluctuations could be expected at various parts of the system, and the oscillograms obtained gave results which confirmed closely the calculated figures. With this equipment we were able to measure rapidly what was happening on all parts of the system, and from the measurements taken we came to the conclusion that it had to be reinforced, with the result that the furnaces are now being supplied from a separate Grid point.

Mr. E. R. Freeman: The paper shows what happened in a particular arc-furnace installation. Having proved the extent of voltage fluctuations, I want to consider how to obviate them. I shall not try to outbid previous speakers about the future size of furnaces or distribution-system fault level; there is still the problem of the firm with a small supply which has to make the best use of it. Where voltage fluctuations are concerned, the series capacitor seems the most elegant solution. It takes out the effect of line and distribution transformer reactance just at the right point. It does, of course, increase the fault level on the furnace-transformer switchboard, but this would also occur with a separate service from the Grid system.

If the series capacitor is to remain in service when it is most wanted, it must be large enough to accept the peak current surges. Series capacitors are less robust than the other units of the distribution system, such as transformers and switchgear, and the time settings for their protective devices are a matter of cycles rather than seconds. The curves in Fig. 6 are on a very small time scale, and I would like more information about the very-short-time current surges.

A series capacitor is used on an arc-furnace load near the north-east coast and has proved satisfactory. Information about any other such installations would be most useful.

Mr. W. L. Brown: Reference has been made to the question of astronomical surge voltages. In the past two years I have known of two failures, both on 11 kV systems. One was a flashover in oil of nearly 5 in, and the other a surface flashover on a furnace-transformer tapping board. As a result of the latter I made up a set and simulated a flashover. The r.m.s. flashover voltage was nearly 80 kV, and I obtained a peak flashover of approximately 150 kV. If we take Mr. White's figure of 20/22 times the voltage V_n, we get approximately 135 kV for an 11 kV system. No entirely satisfactory solution to these flashovers has so far been found, and there is justification for further research into furnace-transformer operating phenomena, for these faults can be very costly matters.

These flashovers, together with others officially recorded, prove conclusively that exceptionally high surge voltages can be initiated and that the generally accepted practice of insulating for the next-higher voltage rating may prove inadequate.

Mr. T. W. Berrie (*at Newcastle upon Tyne*): One of the main duties of the supply engineer is to ensure that the particular load of any one consumer does not unreasonably interfere with the supplies of electricity to another consumer. Experience in this area has shown that, with the rapidly fluctuating loads of the electric-arc furnace, this condition is not at all easy to obtain. From reading the technical Press it is well known how, in the vicinity of this type of furnace, abnormal voltages may be produced together with the annoyance which can be caused by the flickering of lights. We have had some bitter experience of the latter phenomenon in this area.

Much discussion has taken place during the last few years in the technical Press concerning the percentage of flicker voltage which should be tolerated at the common supply terminals of a number of consumers. It appears to be the practice in America to accept about $1\frac{1}{2}$–2% flicker voltage; but, after some study of the problem, I do not think that one can make an exact comparison between American and British practice, owing to the different nature of the supply networks in each country and also to the different meanings attributed to the expression 'common supply terminals'. In many parts of America, for example, the supplies to industrial consumers and the supplies to network substations can be separated, for topographical reasons, to a much greater extent than is possible in this country.

A good working rule for this country when considering supplies to electric-arc furnaces, or for that matter any rapidly fluctuating load, is to work to about 1% flicker voltage at the common supply terminals where other consumers can be affected and up to about $1\frac{1}{2}$% in all other cases. To attain this we have found that the system short-circuit rating should, depending on circumstances, be at least about 100–150 times the rated capacity of the transformer supplying the arc furnace in the first case, and at least about 60–100 times in the second. By means of this simple rule it is possible to decide at what point in the supply system the furnace load should be connected, both with respect to system voltage and the proximity of a power station or an injection into the system from a higher voltage. For example, a furnace of the type and size of that mentioned by the authors should obviously be connected to the system either at 132 kV or, if at 66 kV, where the latter system is reinforced either from a power station or a Grid transformer station, i.e. where the system short-circuit rating is of the order of 1 500–2 000 MVA.

The main variant in any working rule of this nature is the actual magnitude of current peak in the furnace when compared with the full load rating of the transformer supplying the furnace. Are the Figures given in the paper for current variations typical for this size of arc-furnace transformer?

In Section 2.2.1 variations in the 11 kV supply voltage of about 11% have been measured. Using the data given in the paper, my calculations show that this is possible, and I would like to ask the authors to what part of the system the voltage variations of 2–3% mentioned in the Section refers. Variations even of 2–3% would be serious, if they were at the common supply terminals with any consumer; 11% would be disastrous.

* SCHWABE, W. E.: 'Experimental Results with Hollow Electrodes in Electric Steel Furnaces', *Iron and Steel Engineer*, June, 1957, p. 84.

A second important variant upon any working rule for the supplies to electric-arc furnaces is the effect upon the results of the shape of the arc waveform. A third is the effect of the suddenness of each rise or fall in arc current and the frequency of such changes. I do not think that these considerations, especially the effect of the suddenness of each rise or fall of arc current, have been sufficiently considered in calculations which have been made before a furnace has been installed. Most calculations have proved to be pessimistic in practice.

Mr. W. L. Harrison (*at Sheffield*): The installation chosen by the authors for their tests consists of a 60-ton arc furnace, 15 MVA, 11 kV/325 voltage transformer and 11 kV bulk-oil circuit-breaker, which can, in the event of the normal 66 kV supply to the furnace being switched off, be connected via a coupling switch to the works 11 kV ring-main system. To complete the power circuit to the common point of supply a 15 MVA 66/11 kV distribution transformer is employed. The authors, presumably, were only concerned with current and voltage variations on this distribution transformer. The circuit arrangements for this installation are special to the requirements of the user and should not be regarded as typical.

Site tests by the furnace manufacturer when the fault power of the 66 kV system was low, show that there is a total percentage voltage reactance of 60% in the overall circuit. With the electrodes dipped into a liquid bath of metal and with maximum low-voltage transformer tapping, the short-circuit current drawn was 167% of the full-load value, and gave a voltage drop of 24% at 11 kV and 5% at 66 kV. If these steady voltage drops are related in terms of percentage voltage flicker, where the frequency of current fluctuations is in the range 5–12 c/s, we get values of 7·2% at 11 kV and 1·5% at 66 kV. These later values have been obtained by multiplying the actual percentage voltage drop on short-circuit by 0·3, which, from experience, has proved to be reasonable for calculation purposes. It should be appreciated that, when the electrodes are short-circuited by falling scrap within the furnace, extra resistance is added to limit the flow of current. With smaller furnaces rated at around 1 MVA and having a total percentage voltage reactance of 45%, current surges occur which swing by ±100% of the full-load value and reach peaks of 200% at times during the breakdown period. In the case of a particular 60-ton furnace, currents swings during the breakdown period are about ±25% of the full-load value. Greater current swings can occur and for longer periods when the scrap charge has melted sufficiently to form a sizeable pool within the furnace. At this stage of the melt the electrodes can be more effectively short-circuited by the liquid and partially-melted scrap and so cause greater currents to flow; this is shown in Fig. 7 of the paper. Fortunately, the condition appears to last over a very short time in the melting cycle.

Ramsaur and Treweek's factor[9] of 100 times the furnace rating for the system minimum fault power suitable appears to be reasonable for ratings of up to 1 MVA, since the small furnaces are lively and will draw 2–2·5 times the full-load current quite readily. As the furnace ratings increase, this factor of 100 can be reduced without causing lamp flicker. It is quite true that, if a factor of 100 were applied to all existing furnace installations more than 50% would have to be disconnected. In fact, many equipments are working quite satisfactorily, the percentage voltage flicker produced being within the tolerable limit of 1%.

With the increased fault power at 66 kV raised to about 1 000 MVA, a furnace 15 MVA load has a voltage flicker of 0·75% at 66 kV, while another furnace 20 MVA load, which has a total voltage reactance of 40% in the overall circuit, has a flicker of 1·5%. I am not aware of any reports of lamp flicker as a result of the operation of these two furnaces.

W. E. Schwabe* suggests that the present accepted maximum value of 1% is conservative. From experience gained with two installations in conjunction with the above method of calculation it seems that practice has proved this.

Mr. H. J. Sheppard (*at Sheffield*): I would draw attention to Ramsaur and Treweek's recommendation,[9] illustrated in Fig. 1, which indicates that the short-circuit power should be approximately 100 times the furnace-transformer rating for a 3 MVA furnace, diminishing to about 40 times the furnace rating for a 20 MVA furnace.

I would also draw attention to a more recent American paper,† which includes curves showing the maximum permissible system reactances (from the supply source up to the point at which other loads are connected) for single-furnace, two-furnace and multiple-furnace installations. It is based on returns from some 18 electricity supply undertakings, stating whether the voltage flicker caused by a large number of furnace installations can be regarded as non-objectionable, border-line or objectionable. There is considerable scatter of the points plotted, and it is indicated, for example, that a particular installation of two 20 MVA furnaces is being satisfactorily supplied from a system fault level of 1 100 MVA, while, on the other hand, objectionable interference is experienced on a different system when two 25 MVA furnaces are supplied from a fault level of 5 000 MVA. These two examples illustrate the measure of uncertainty which, at present, faces the engineer who is called upon to design a system to provide arc-furnace supplies. This paper concludes that the condition of cyclic flicker (fluctuations at the rate of more than one per second) appears to become important for some furnaces at ratings above 15 MVA and perhaps at electrode voltages higher than 350 volts.

Curves have been published showing a relationship between the frequency of fluctuation and the extent of voltage variations, which are such as to be visible and those which cause irritation.‡ The limit of what is visible varies considerably from one person to another, and I believe that the limit of what causes irritation is even more variable.

At any point on a supply system from which large numbers of consumers are supplied, the voltage fluctuations should be so limited that they are not visible to the vast majority of people. The means by which this can be achieved are as follows:

(i) Increasing the fault level by reinforcing the system.

(ii) Segregating the furnace supply from other supplies, in order to make the connection to a common point at a higher fault level. This usually entails connection to a busbar at a voltage higher than that required for supply to the furnace transformer and the provision of a step-down transformer solely for the furnace supply.

(iii) Reducing the load fluctuations imposed on the supply system by meeting part of the fluctuating demand from another source, namely a synchronous condenser associated as closely as possible with the arc furnaces.

It is often necessary to use a combination of these methods.

The provision of a separate supply system to deal with large arc-furnace loads has been suggested, but this would entail the duplication of power stations and transmission lines, which would not, in my opinion, be either economically or physically practicable.

Dr. B. C. Robinson and **Mr. A. I. Winder** (*in reply*): In reply to Mr. Sedden, whilst we have had no experience of the use of synchronous condensers to avoid flicker, it would appear desirable that the machine should have a low reactance to enable it to supply a high fault current for short periods, thus compensating for the current surges in the furnace. It can also be

* SCHWABE, W. E.: 'Lighting Flicker caused by Electric Arc Furnaces', Paper presented to the 1957 A.I.S.E. Convention.

† 'Survey of Arc Furnace Installations on Power Systems and Resulting Lamp Flicker', *Transactions of the American I.E.E.*, 1957, 76, Part II.

‡ E.R.A. Report Ref. K/T110.

used to improve the power factor of the combined installation. The improved or even slightly leading power factor would also cause the voltage fluctuations to be less noticeable. Static condensers could be used to improve the power factor and would be of use in reducing the transients of higher frequency. They would probably have little effect on the flicker voltage except through an improvement of the power factor.

The comparator voltmeter circuit appears very ingenious, but on examination it appears doubtful whether it would show all the voltage variations and excess voltages measured during our tests. Fig. G is an enlargement of Fig. 5(*c*) just after the arrow. The voltages do not increase and decrease symmetrically in the two half waves. Mr. Sedden's circuit cannot show these, but only a mean value. The time of the response of the comparator voltmeter will be limited by the time-constant of the 16 μF condenser circuit, and therefore it would not be suitable for the tests described in the paper since the primary object was to detect and measure excess voltages. The instrument would thus appear to be suitable for comparing flicker voltages, but, owing to frequency limitations, its response might vary with the flicker frequency.

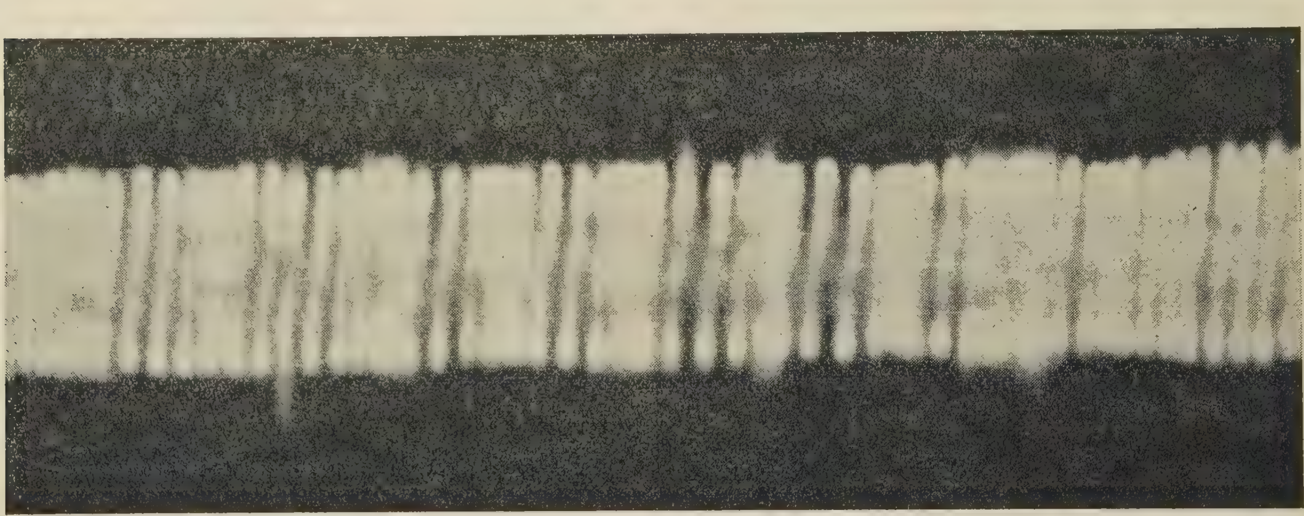

Fig. G

In reply to Mr. May, the majority of the tests, except those shown in Figs. 3 and 15, were carried out after the modification of the electrode control system.* The voltages in Figs. 11 and 12, and also the waveforms in Fig. 9, were measured at the electrode clamps and not at the transformer terminal as used for the electrode control system. The latter voltage is shown in Fig. 10.

Mr. White mentions switching surges as being a possible cause of over-voltages and concludes that the oscillograph equipment was not capable of showing these. Switching transients were recorded when the furnace transformer was switched on and put on load. No attempt was made to record line-switching transients, since they were beyond the scope of the investigation.

In over 600 random oscillograms of the arc voltage, no abnormal peaks have been observed except those similar to Figs. 9(*b*) and 9(*f*), which represent some of the higher voltage oscillograms.

The suggestion of thermionic emission causing arc stability was based on two pieces of evidence: (i) stable arcing only occurred when the charge was molten, i.e. hot, and (ii) Fig. 9(*c*) shows normal arcing taking place only with the charge positive, and this resembles the voltage across a half-wave rectifier.

We regret that we cannot give Mr. Hawley any information on the power factor of the load beyond Mr. May's information that it is about 0·8, since no measurements were made.

In reply to Mr. Payne, no quantitative measurements of light flicker were made. The method of measuring the oscillograms was to put the film in a photographic enlarger with about 5 diameters magnification. Two lines were then drawn showing the width of the voltage band when the furnace was off load. The film was then moved through the enlarger and any significant over-voltages were then noted in time and magnitude. Smaller over-voltages were too common to be counted.

Cyclical variations of voltage with a frequency of about 40 cycles per minute can be seen on the record of the arc voltage, as shown in Fig. 11. These would appear to be connected with the disturbances noted by Schwabe. A variation of current of nearly this frequency is also shown in Fig. 6.

Mr. Berrie asks whether the diagrams of current variations given in the paper are typical of this size of furnace. Unfortunately this is the only furnace of this size on which we have any information. Fig. 8 was chosen as being the highest current recorded. Fig. 7 is a particularly good example of load swinging between time 82 and 84 min. Otherwise the records are typical of many feet of records we have taken. Of course, as the energy input into the furnace was reduced in the latter stages of the melt the currents become steadier.

The effective impedance between the power stations and the 11 kV busbars represented about 15% or 43·8 equivalent ohms on the 66 kV system. A current of 500 amp passing through this would give a voltage drop of 2·19 kV or 3·3% reactive feeder and transformer voltage drop. It is not possible to calculate the drop in the terminal voltage on the 11 kV busbars since the load power factor is unknown. The 11% represented the actual voltage drop measured from the oscillogram on the 11 kV terminals. Owing to the relative line and transformer impedances, which are both assumed to be purely reactive, the voltage drop on the 66 kV transformer h.v. terminals will be 11 × 0·17% (see Section 4.1) or 1·8%. Since the next substation is at a distance along the line one might estimate a voltage variation there of 1·5 or 1·6%.

* Described in *Electrical Review*, 1957, **160**, p. 7.

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THE PLANNING AND CONSTRUCTION OF LARGE MODERN THERMAL GENERATING STATIONS

By JEAN PIMPANEAU.

(ABSTRACT *of Lecture delivered before a joint meeting of the* SUPPLY SECTION *of* THE INSTITUTION *and the* BRITISH SECTION *of the* SOCIÉTÉ DES INGÉNIEURS CIVILS DE FRANCE 18*th December*, 1957.)

THE PROJECT

In France the decisions to build thermal power stations rest with the Planning Commission, and the present programme ('Third Plan') covers projects up to 1965.

The objectives are to provide for an annual energy consumption, in 1965, of about 110×10^9 kWh (compared with 50×10^9 kWh in 1955), with a peak load of about 22000 MW (compared with 9000 MW in 1955) and a firm continuous available capacity of 17000 MW during the winter months. The thermal generating sets now standardized by Électricité de France have a normal rating of 115 MW and a maximum rating of 125 MW. Such a set can provide a firm capacity of 86 MW to meet the consumer demand in the winter months and a peak capacity of 95 MW, the annual energy output to consumers being about $0 \cdot 5 \times 10^9$ kWh.

With these objectives and characteristics, the problem is to make an appropriate choice of thermal and hydro-electric stations (with the possibility of nuclear plants in the immediate future) to satisfy the requirements.

PRINCIPAL ECONOMIC FACTORS OF A THERMAL STATION

The financial charges which have to be borne by a station during its life (estimated at 30 years by É. de F.) may be classified as the initial cost of the station and its equipment (establishment cost), the fixed operational costs, and the running costs.

If the thermal station is equipped with four 115–125 MW sets, the size of the buildings, ground area, arrangements for fuel handling and storage, cooling water, etc., will be known, and an estimate of the initial cost of the station buildings, etc., can be made. This will be about 20% of the total charges to be debited to the station during its life.

The fixed operational costs include salaries and wages of operating and maintenance personnel, engineering and administrative staff, rates, taxes, etc. They represent about 10% of the total charges to be debited to the station.

The running costs cover the fuel consumed and may represent 60% of the total charges to be debited to the station.

Since the load centres are usually remote from the station, the cost of the transmission lines and the losses therein will be a charge on the station. For a 225 kV, 50 km line, these charges may represent 5% of the total charges to be debited to the station.

PROBLEMS CONNECTED WITH CHOICE OF SITE

The choice of the location of a thermal station involves considerations of terrain, reception and storage of fuel, source of cooling water, etc.

A total ground area of 100–220 acres may be required for a large station, to provide for the main buildings, switchyard, coal storage (400000 tons), coal wharf, railway sidings, oil store, etc. Considerable efforts have been, and are being, made to reduce the ground area and volume of the principal buildings, which are preferred to be of the single-storey type to reduce the cost of the building and foundations. Research will be necessary to ascertain the suitability of the subsoil for heavy buildings. With a riverside station, examination of the records of the flow of the river, flooding, maximum high-water levels, etc., will be necessary for the purpose of deciding upon the requisite civil engineering works to safeguard the basements of the main building against water infiltration, flooding, etc.

A modern station with four 115/125 MW sets consumes about 4000 tons of coal a day, or 10^6 tons per annum, during the first few years of its life. Such quantities may be transported conveniently only direct from the mines to the station. It is therefore important that the station be situated either near a trunk railway line, in a port available to maritime traffic, or on the bank of a river available to large-tonnage barges. It is desirable, in general, that both rail and water facilities be available.

A 4-set station may require about 20 cumec ($15 \cdot 84 \times 10^6$ gal/hour) of water for cooling the condensers. Thus large stations should be placed on the banks of important rivers with a large flow of water—a condition which seriously restricts the choice of site. Restrictions on the permissible upper limit of temperature of the river water may also cause problems associated with a proposed site. Further difficulties may arise if the banks of the river are already occupied by industrial works.

When a river site is impossible, cooling towers must be employed, which may lead to a 3% reduction in the efficiency of the station. The quantity of water required in this case, however, is not negligible and may amount to 57000–66000 gal/hour for a 115–125 MW set.

A portion of the energy may be supplied direct to a local network, but the bulk will have to be transmitted at extra high voltage (225 kV) to distant utilization centres. This transmission is usually by overhead lines, but in exceptional circumstances, underground cables may be necessary over parts of the route. A detailed survey of the route to be taken by these lines is necessary, and this research may influence the decision on the final choice of site for the station when alternative sites are available.

The selected site must receive the approval of the appropriate administrative authority, and this procedure may involve many difficult problems.

The many problems connected with the choice of site are therefore particularly onerous, and since 1948 both the C.E.A. and É. de F. have instituted a systematic research for the establishment of new stations.

CONCEPT OF STATION

We now come to problems which concern the concept of thermal power stations, the technicalities which have a direct interest to the engineer. The economic study outlined in the preceding Sections has thrown into relief the effect on the price of a kilowatt-hour of a number of principal factors which depend essentially on the concept of the station, namely the cost of

* M. Pimpaneau is with Électricité de France (Equipment Department).
† The lecture is based on M. Pimpaneau's paper, 'Conception des grandes centrales thermiques modernes' (*Mémoires de la Société des Ingénieurs Civils de France*, 1956, No. 5, p. 448).

buildings and equipment, the thermal efficiency and the effectiveness of the station.

Thermal Efficiency

Since the fuel represents 60% of the total costs debited to the station, all efforts and research should be made to reduce the specific fuel consumption by improving the thermal efficiency. The efforts of the manufacturers in association with É. de F. are known and have resulted in: increased steam pressures and temperatures at the turbine stop valve; improved heat cycle by the general adoption of reheating and bleeding; improvements in the combustion chambers of the boilers; reduction of the exit temperatures of the flue gases; improvements in feed-water heaters and superheaters; improvements in turbines and increased power of units, resulting in increased mechanical efficiency; reduction in the losses of the main alternators (due to hydrogen cooling and improvements in the magnetic qualities of the core laminations).

Equal efforts have been made to increase the efficiency of auxiliary plant, which absorbs about 6% of the output of a turbo-generator unit. Steam-driven auxiliaries have been completely abandoned. The motors are usually supplied at low or medium voltage from a transformer connected directly to the alternator. Motors with single or double-cage rotors and direct starting are employed whenever possible.

All É. de F. stations commissioned during recent years have benefited from the efforts mentioned. The net specific consumption of the station is about 2 300 kcal/kWh at the economic load, which means about 2 600 kcal/kWh in everyday operation. In comparison, the net specific consumption of stations commissioned immediately after the last war was 2 900–3 300 kcal/kWh.

Civil Engineering Works—Principal Buildings

Since the establishment cost represents 20% of the total charges debited to the station and is the first disbursement of capital to be met on completion of the station, it is natural that this cost should be the object of vigilant attention of all concerned. The planning of the main buildings, foundations, revetments, culverts, etc., must therefore receive careful study so as not to incur unnecessary expenditure.

Although some of the older stations in the Paris region were constructed with more than one floor, the present-day practice is to place all heavy equipment on one floor (the horizontal concept) in order to distribute the load on the foundations and to reduce the cost of the superstructure. For example, at the Porcheville station, vertical supporting walls are required only for the boilers, coal bunkers, travelling crane, feed-water tanks. Some researches are actually in progress to reduce or limit the corresponding expenditure.

The horizontal concept, with all equipment on one floor, requires a relatively large ground area for the principal buildings and has led to the idea of placing the boilers and main equipment out of doors, as at Bordeaux. Separate foundations are provided for the several items of equipment, and these are interconnected by light foot-bridges.

Principal Machines and Auxiliary Plant

The cost of this equipment may represent about 80% of the establishment cost, and it is on these items that the policy of É. de F., in its endeavour to lower the price of a kilowatt-hour, is more apparent. Engineers charged with the study of stations and their layout, utilizing all up-to-date technical improvements, have also to consider the progressive developments in the characteristics of the machines and the heat cycle. This leads to a multiplication of the types of machines over a period of years.

In France the Director of Equipments has fixed technical levels and has standardized a generating unit of 115–125 MW, of which 40 will be built by the principal manufacturers, 10 identical machines having been ordered. The development costs of the prototype machine will be spread over these machines, and the prices of future machines may therefore be reduced. Such standardization has been effected at Creil and Porcheville, each of which contains four identical units (boilers, turbines, alternators, etc.).

Standardization is also applied to the principal auxiliary machines (which are required to have identical characteristics) and to the operating voltages of the driving motors, namely 5·5 kV for motors of 150 h.p. and above, and 380 and 220 volts for motors below 150 h.p.

Operating Staff

The operating staff required for a modern station is equivalent to 0·4 man/MW, compared with 4 men/MW for old stations. This large reduction in personnel is due to the combined efforts of the equipment services (by the adoption of new ideas) and the exploitation services (by reorganization).

The first step to ensure the correct functioning of a station with reduced operating staff is to reduce the number of machines by making the output of each unit as large as practicable. At the end of the last war the largest machines were 50–55 MW. Now machines rated at 115–125 MW are installed, and some machines rated at 250 MW have been ordered.

All new plants now operate on the unit system, i.e. each unit (turbine, generator, boiler and auxiliaries) has no connection with the other units in the same station, although the control panels for adjacent units may be installed in the same control room. The operating staff is thereby reduced.

The handling of coal is the second largest consumer of hand labour in a thermal power station. To-day, coal handling is effected by one shift, but in the past two or three shifts were necessary. Thirty men may be required over a period of eight hours.

THE FUTURE OUTLOOK

The objectives of the Third Plan which have already been considered (namely the obligation of É. de F. to provide, between 1957 and 1961, steam stations with an installed power equivalent at least to forty 115–125 MW sets) and the tasks involved in their completion are very considerable and urgent. The programme in hand, based on sets of 115–125 MW, will be followed actively.

The outlook for the future is relatively bright. It is certain that the fixed operational costs will be reduced, owing to the increase in the rating of the plant unit from 125 to 250 MW or more. The cost of civil engineering works in the construction of the principal buildings should also decrease, owing to the efforts of the engineers responsible for their design and construction, and the employment of up-to-date techniques and methods. The cost of the machines, however, represents a very high proportion of the first establishment cost, and it is on this item that the principal research should now be directed. This effort should be towards improvements which will lead to a lowering of the specific consumption and a corresponding reduction in the fuel costs.

IMPROVEMENTS IN THE HEAT CYCLE

In the ideal heat-engine cycle proposed by Carnot, the thermal efficiency depends on the temperature limits, the lower temperature being that of the cold source (e.g. the condenser) and

the upper temperature being that of the steam at the stop valve.

The temperature at the condenser is fixed by that of the cooling water, and therefore to increase the efficiency of the Carnot cycle the upper limit of temperature must be raised to the highest practicable value. With fixed temperature limits the efficiency of a steam cycle is improved by raising the pressure of the steam entering the machine. Hence the best efficiency occurs at very high pressure and temperature.

Data on the relationship between specific consumption, temperature and pressure have been compiled by Chadwick and the American Gas and Electric Service Corporation (A.G.E.S.C.).

Chadwick's data show that the specific consumption at 750° F and 400 lb/in^2 is 13 500 B.Th.U./kWh net; at 1 000° F and 1 500 lb/in^2 it is approximately 10 000 B.Th.U./kWh net without reheat, and 9 500 B.Th.U./kWh net with single reheat; while at 1 100° F and 2 400 lb/in^2 the figures become 9 300 and 9 000 respectively. Extrapolation of the curve to 1 600° F and 8 000 lb/in^2 shows a specific consumption of about 8 000 B.Th.U./kWh net with double reheat.

The A.G.E.S.C. data show that, at 1 050° F and 2 000, 3 500 and 4 500 lb/in^2, the specific consumptions are 8 900, 8 680 and 8 590 B.Th.U./kWh respectively with one stage of reheat, and 8 835, 8 587 and 8 555 B.Th.U./kWh with two stages of reheat; for 1 200° F and the same pressures the figures are 8 630, 8 375 and 8 300 BTh.U./kWh, and 8 500, 8 220 and 8 130 B.Th.U./kWh respectively. These figures show that at 1 050° F it is useless to increase the pressure above 4 000 lb/in^2, but at 1 200° F a progressive reduction in the specific consumption takes place at pressures up to 4 500–5 000 lb/in^2, particularly with two stages of reheat.

Research into the adoption of increased admission pressures and temperatures should be pursued with tenacity. The evolution of a high efficiency for a thermal station without incurring heavy establishment costs and without compromising the reliability of operation manifests itself already throughout the world and notably in England. This evolution interests the metallurgical, mechanical, electrical and chemical industries, design offices, laboratories, etc. It is therefore in justification of the title that so much importance is attached, in large industrial countries, to methods of producing large amounts of energy.

DISCUSSION BEFORE THE JOINT MEETING OF THE SUPPLY SECTION AND THE BRITISH SECTION OF THE SOCIÉTÉ DES INGÉNIEURS CIVILS DE FRANCE, 18TH DECEMBER, 1957

Mr. F. H. S. Brown: The basic problems involved in Britain and France are alike, although the C.E.A. system is the larger; but even so, it is surprising that the solutions found are so similar. To point to some of the differences: in this country it is rarely possible for inland stations to be supplied with fuel by rail and water, and rail only is the usual method, because of our small rivers. Related to this is the fact that, because the dry-weather flow of the largest of our rivers is insufficient for a 500 MW station, it is seldom possible to site inland stations with direct cooling. Even the provision of cooling-tower make-up water is becoming difficult, and the C.E.A. have therefore recently decided on a trial installation of a dry cooling tower of 120 MW capacity employing jet condensers instead of surface condensers. The warm water from these will be circulated through a system of large heat exchangers located round the base of a natural-draught cooling-tower shell. No heat is lost by evaporation, and consequently about three times as much cooling air will be needed, resulting in increased tower dimensions and spacing. Even allowing for possible savings in fuel transport costs if such an installation permitted siting a station nearer its supplying collieries, the dry tower is likely to be appreciably more costly than a normal water-cooling system.

The French development of the total outdoor station will be watched with great interest, because although we have four semi-outdoor stations built or building, we are not yet convinced of the overall advantage of putting the turbine outside.

A large measure of standardization has taken place in both countries, but an interesting feature is the rapid growth in the size of the units being installed by the C.E.A., who feel that the economic returns given by this increase in size warrant quite a large degree of departure from a policy of complete standardization.

Once again there is a similarity between the standardization of auxiliaries in the two countries. It seems possible that we may be forced to supply these at 11 kV for the very big stations and very big units, and I should appreciate the author's views on the appropriate methods of driving the feed pumps of these large units. We are currently thinking that for these conditions it is a commercial proposition to revert to a form of direct drive to the boiler feed pump, and either have the steam turbine tapped across the main unit or by mechanical drive from the main turbine.

The author concludes with a discussion on the future outlook and improvements in the heat cycle. He is entirely right in his emphasis on the desirability of increasing thermal efficiency and in his discussion on some of the factors involved. We in Britain feel that we are now entering the sphere where neither thermodynamics nor, indeed, simple economics are necessarily the predominating factors. The trends of economic returns from further increases either in the size of units or in steam conditions are flattening; nevertheless, the possibilities of further gains must be explored. However, in Britain it seems virtually certain that the large-scale application of nuclear power, with its higher capital cost and lower running costs, will mean that all new conventional units must be capable of 2-shift operation at a very early stage in their career. Inevitably this means some slowing down in the rate of progress and an adjustment in outlook on plant development to take account of a whole series of new factors. We are actively considering these factors and think that some further development is definitely possible. It would be most interesting if the author could give some indication of what developments the French anticipate in the not too distant future.

Mr. D. B. Irving: There are one or two points in the paper on which I should be glad to have further information. Mr. Brown refers to nuclear stations, and I, too, should like the author to give some indication of the part they will play in the French programme. After all, a nuclear station is a *centrale thermique*.

I note with interest that the staffs have been reduced from 4·2 to 0·4 employees per megawatt. I should like to know how this was achieved, and whether it was done with the full co-operation of the trade unions.

In Britain we have only to propose to erect a power station or overhead line and there is an avalanche of objections, despite the fact that all admit electricity to be necessary. Is the same thing experienced in France?

Mr. F. J. Hutchinson: Aerial survey and geophysical ground tests can greatly reduce the time taken in site selection. In the matter of combustion efficiency, attention is drawn to American claims of $\frac{1}{4}$–$\frac{1}{2}$% improved efficiency for the pressurized furnace;

and also to the very large reduction in fly ash when slagging or 'wet bottom' furnaces are used.

I note that the turbines appear to have been standardized for steam at 1 850 lb/in^2 and 1 010° F; in recent investigations I have been unable to foresee any worthwhile improvement in cost per unit sent out using higher steam pressures and temperatures. In regard to control rooms, I feel that not more than two large units should be controlled from any one room. Fire risk is always present, and the cost of a prolonged outage of, say, 250 MW of plant due to a fire near a control room could not be tolerated.

In the matter of staffing of power stations, I think that the author's figure of 0·4 man/MW of plant could probably not be achieved at a 500 MW station in this country if the whole of the station staff, as distinct from the operating staff, is included. However, I believe that the centralization of coal-handling plant can produce striking reductions in manpower. At a station in West Virginia, U.S.A., the central control of the coal-handling plant needs only one operator to supply coal to bunkers or to stock and normally from stock to bunkers.

Referring to Chadwick's figures on the thermal cycles and the possible heat rates for large units with advanced steam conditions, great caution is needed when making extrapolations. Although some American stations had achieved heat rates of a little over 9 000 B.Th.U./kWh sent out with an availability (on the American basis) of over 85%, we in Britain had so far not done better than about 10 800 B.Th.U./kWh sent out and 80% availability. However, I feel that this figure will soon show a marked improvement with the large modern plant that is now coming into reliable commercial use.

Mr. W. J. Price: Although the economies of France and Britain are fundamentally different, nevertheless, from a restricted technological viewpoint, similar problems exist and answers arrived at in both countries exhibit a large measure of agreement. In both countries the 'fixed running costs' and 'initial establishment charges' are being reduced by increasing the capacity of individual units, thereby reducing both the station personnel and the building volume. At a Canadian station where the organization with which I am associated has installed an initial 66 MW set, the comparable figure has been given as 0·25 man/MW. The adoption of outdoor or even semi-outdoor stations in Britain is questionable, and cheap cladding should prove to be a better economic proposition. Further savings on buildings could be realized by installing duplicate conveyors with smaller-sized bunkers and feed-water reserve tanks at ground level. The reference to cooling towers prompts me to question the wisdom of using the natural-draught type in Britain and France, knowing that the Americans wholeheartedly employ mechanical draught.

Recent C.E.A. figures show an improvement in system efficiency of 19% over the past nine years, coupled with an increase in fuel costs of 42% which underlines the uncertainty of long-range mathematical prediction. This aspect of power-station economics has recently been dealt with by Norman Elce.* Broadly speaking, a 120 MW turbine consists of the hot end, the middle, and the cold wet end, which respectively produce ¼, ½ and ¼ of the total power generated. The middle section is capable only of relatively minor improvement, and so designers point always in the direction of higher steam conditions, whilst neglecting to some extent the cold wet end—which is still capable of yielding worthwhile gains.

There is a need for a comprehensive review of the economical aspects of the sub-atmospheric portion of the turbine, comprising blading, condenser and cooling-water system. Such a study conducted on a regional basis could afford measured results which would be of great value to the relatively low-pressure and low-temperature nuclear power stations.

Finally, there should be more enthusiastic efforts to use some of the vast quantities of low-grade heat now being lost in cooling-water systems. Such efforts should be made on a local basis and might range from minor district-heating schemes to market gardening on a big scale.

Mr. A. W. Pedder: There is much in the lecture that one must agree with and very little that needs comment, mainly because the author has not disclosed many quantitative data; but there are one or two stated conclusions that are open to question. For instance, the standardization of auxiliary voltages at 5·5 kV and 380 volts is mentioned. I feel that this would have to be reconsidered for the new stations with very large sets, because it is probable that the use of three voltages would be more economical.

Again, steel frames for main buildings are referred to as 'standard'. Both in Britain and abroad a number of very economical reinforced-concrete turbine houses have been constructed: it all depends on the particular design of building. To what extent have Électricité de France been influenced by the light steel frame shown in a slide, the lightness being largely made possible by the fact that the capacity of the turbine-house crane was kept down to the small figure of 30 tons?

The 3% loss when using indirect cooling, i.e. cooling towers, is a bit high unless really cold water is available, and particularly unless the design of the turbine exhaust area is such that that cold water can be fully used. Do É. de F. envisage that their future large turbines will be cross-compound, which would enable them to get more benefit from the cold water than from the tandem machines commonly used for the higher water temperatures?

The equation of basic economics becomes interesting only when you begin to fill in the figures and apply it to a specific case. I should be very interested to know the author's figures for the 250 MW turbines just ordered, and also the steam conditions adopted for them. The classical example of such analytical treatment is given in the paper on optimum condenser sizes written by Bottomley; his work has had to be extended to deal with conditions not then envisaged, but it remains an admirable introduction to the method of finding an optimum solution. However, especially when dealing with substantial developments in manufacturing technique, any equation must be modified by judgment, since it is scarcely possible to bring into it allowances for the possibility that really advanced design might at some point involve greater risk of outage, at least in the earlier stages of the station's life. A closely related problem is that of allowing for increases in size of the largest units on the system. One 250 MW set is not of exactly the same value to the system as two 125 MW sets; but the equation as it stands is incapable of taking this into account. In fact we have a paradoxical situation: the more we need these equations to assess major advances in design, the less use they are, because they require more and more judgment in their application. Moreover, the benefits shown by such studies show diminishing returns and we have to exercise more judgment to consider whether they justify the new advance.

Mr. R. F. Alexander: The author mentions that 20% of the total cost of a 500 MW thermal station is expended on civil engineering, and this proportion is also about right for Britain. The organization with which I am associated is building two power stations in this country, one thermal the other nuclear, the respective capacities being 360 and 340 MW. The steam conditions for the thermal station are 1 500 lb/in^2 and 1 000° F, the circulating-water requirements being $10 \cdot 5 \times 10^6$ gal/hour;

* ELCE, N.: 'Economic Basis and Character of Steam-Turbine Design', *Proceedings of The Institution of Mechanical Engineers*, 1956, 170, p. 1009

those for the nuclear station are 320 lb/in^6 and 672° F, but the circulating water requirements are 21 × 10^6 gal/hour, owing to the much lower steaming conditions. The cost differential between the two power-houses is almost entirely due to the greater circulating-water demands of the nuclear station.

I agree with the author that all heavy equipment such as feed pumps and induced draught fans, and structural features such as boiler flues, should be placed at ground level. Forced-draught fans are better sited on the boiler framework, which must in any case be a massive steel structure to carry the weight of the boiler and the overhead bunkers.

The author mentions that in France the station personnel have been reduced to 0·4 man/MW; I believe that the figures for Britain and America are 0·6 and 0·2 respectively. We are at present designing a 900 MW thermal station for North America for which it is hoped that the total personnel required will be less than 0·2 man/MW, but this needs qualification in that the boilers will be gas-fired.

Mr. B. A. E. Hiley: All who are interested in steam generating stations are constantly alive to increasing efficiency and reduction in costs—either capital or running, or both.

As Mr. Brown states, developments in France and in Great Britain are to a great extent parallel. By 1948 we had achieved some 22% efficiency; to-day Drakelow 'A' and Stourport have reached 30·73% steaming at 1 650 lb/in^2 and 1 050–1 060° F. Unit sets, i.e. one suspended boiler and one generating set, have now been employed for some years. All this has saved in area and volume of building structure, and for this thanks must go to the metallurgist to a great extent.

Another development concerns cooling plant. Where a cooling circuit cannot be closed with a river, canal, dock, etc., cooling towers of the natural-draught type have been found to be the most economical solution. However, these use great quantities of water—some 15 × 10^6 gal/hour for a 360 MW station. Mr. Brown refers to the dry cooling tower which is under development in this country. If this is successful, one further great economy in pumping, screening, etc., will be effected, and I am sure that our French friends will be interested.

One of the troubles in, say, a 480 MW coal-burning station is the disposal of some 700 tons of fly ash per day. Where coal is used a very large area of land is required to dispose of this ash, the area being proportional to the depth to which ash can be deposited. In selecting a site, therefore, one should include in the investigations river or similar sites where there exist sands and gravels, and land that is low lying and liable to flood; not good agricultural land. The station should be placed on the higher contours, the sand and gravel being excavated for use in building and such excavations filled in and the general ground level raised above flood level with the station ash. The top soil can be returned and the land used for agricultural or industrial development. The large areas of land used for coal-burning stations can then be justified economically. Hams Hall stations 'A', 'B', and 'C', Drakelow 'A' and 'B' and Uskmouth 'A' and 'B' are typical examples of this.

If oil becomes the established fuel for stations, a small tank farm and a pipeline may well replace the large marshalling yards, bunkers, etc. This will also save a large area of land.

THE AUTHOR'S REPLY TO THE ABOVE DISCUSSION

Monsieur J. Pimpaneau (*in reply*): I propose to reply by subject rather than to individuals.

Estimates of Power-Station Costs.—We recognize that any economic calculations on projected installations must be based on the assumption that the data available will be valid in the future. The actuarial method used to compare the relative costs associated with the future power station—construction costs, fixed and proportional operational costs—presents interesting characteristics which deserve comment from the aspect of the probable accuracy of the forecasts.

We require to find the amount, F, which, invested at a rate of interest r the day the power station is commissioned, will make it possible to defray these costs each year during the 30 years of the station's life.

If $F_1, F_2 \ldots F_{30}$ are the annual costs of the power station during the first, second . . . 30th years,

$$F = \frac{F_1}{1+r} + \frac{F_2}{(1+r)^2} + \ldots + \frac{F_{30}}{(1+r)^{30}}$$

because it is sufficient to invest to-day at a rate of interest r an amount $F_n/(1+r)^n$ francs to provide F_n francs at the year n.

It can be seen that an error in the estimation of F_n introduces an error dF in F such that $dF = dF_n/(1+r)^n$. However, dF decreases as n and r increase. In view of the rates of interest prevailing at present, we may conclude that the accuracy obtained by this method depends more on the accuracy of forecasts for the first ten years or so of operation than on those for the last 20 years.

Since 1952 Électricité de France has followed a policy of standardizing its thermal stations, and twenty-seven 125 MW reheat sets (1 800 lb/in^2—1 000° F—1 000° F) have been ordered since then; 10 sets are already in operation and another 30 are planned. These standardized sets are well known, and it is clear that the difficulties which may arise in their construction, commissioning and availability during the first and second years are minimized; the relevant economic calculations therefore have every chance of being substantiated by results. This is a good illustration of another aspect of the advantage of standardization and the maintenance of the standards over a long period. The growth of the demand, technical progress, etc., occasionally change standards, but the new standards should introduce substantial progress so that they may be adopted for a sufficiently long period. É. de F. has thus ordered 250 MW reheat sets (2 350 lb/in^2—1 050° F—1 050° F), although the forecasts for these units cannot be as accurate as those for the standard 125 MW sets.

However, the method of economic calculation remains valid, and one may to a certain extent take into account additional uncertainties, namely

(*a*) The construction costs derived from the sum of the tenders submitted by the contractors may be increased to take into account construction delays.

(*b*) The production forecasts during the first and second years of operation of the new units may be reduced to take into account the preliminary troubles which arise in any new power station.

Finally, one knows that uncertainties preventing either the production or the consumption of electrical energy essentially affect power stations more than ten years old and have very little effect on the new power stations, which operate on base load.

It is therefore not unreasonable to state that one can predict without important error, for a given level of prices and during a period of stable economy, the costs incurred by the power station during the first ten years of its life.

In the expression for F it is possible to split the costs into two parts, corresponding respectively to the first ten and last 20 years of the life of the power station: this gives

$$F_1^{10} = \frac{F_1}{1+r} + \ldots + \frac{F_{10}}{(1+r)^{10}}$$

and

$$F_{11}^{30} = \frac{F_{11}}{(1+r)^{11}} + \ldots + \frac{F_{30}}{(1+r)^{30}}$$

from which $$F = F_1^{10} + F_{11}^{30}$$

With practical figures it is possible to show that F_1^{10} is approximately 70% of F.

However, the annual costs, F_n, are the sum of three terms:

(a) The capital costs corresponding to amortization for the construction.
(b) The fixed charges for operating costs.
(c) The proportional charges for operational costs.

The terms (a) and (b) are not subject to great uncertainty. Term (c) calculated for the last 20 years of the life of the power station represents only about 15% of F, so that an error, even an important one in the power-station utilization forecasts over these last 20 years, is unlikely to modify F appreciably.

Choice of the Power-Station Site.—As in Great Britain, we have a set of very detailed maps and aerial photographs covering the whole of France, so that it is easy to find likely sites for power stations. Since the sites are generally beside navigable waterways or large rivers, they are always easily accessible, although, to accelerate surveying, we occasionally utilized the helicopters normally used for the inspection and maintenance of transmission lines.

Station sites are always chosen in areas suitable for industrial development in consultation with all the Ministries concerned and the national or local administrations, and so far—in spite of inevitable difficulties—it has always been possible to reach satisfactory solutions. Often, as in England, local opposition has been met, based on the fear that the new station will interfere with the surrounding area through air pollution or noise. This opposition has often been reduced by visits to recent power stations in operation.

We prefer to site our future stations on rivers which have a sufficient flow to provide direct cooling water to the turbine condensers. In France, the average river-water temperature over the year is 60° F, leading to a turbine exhaust steam temperature of 80° F and an absolute pressure of 0·5 lb/in², allowing for a temperature rise of the river water of 12·5° F and a terminal gradient at the condenser of 7·5° F. These assumptions have made it possible to standardize on 125 MW turbines (3 000 r.p.m.), tandem compound with three exhausts, and 250 MW turbines (3 000 r.p.m.), tandem compound with four exhausts.

In this light, the manufacturers supply turbines with the largest effective exhaust areas compatible with their type of construction (6 m² for 125 MW and 11 m² for 250 MW).

Taking into account the temperature of the river water and the quantity of energy supplied each month by the turbine, the most economic solution for each power station (condenser surface, pump capacities, culvert cross-section, etc.) is determined by balancing capital costs against operational results by the method outlined above.

Power-Station Design.—Most recent power-station buildings have been steel framed for reasons associated with cost, speed of construction and special space and ease of installation. Theoretically, it is possible to impute to the benefit of this construction the gain on marginal interests due to more rapid building. Our position in this connection is not as categorical as it may have appeared in the lecture, and arises only from the circumstances affecting France during these last few years. There may well be cases—due to the station site or the state of the steel market—when reinforced-concrete construction should be adopted.

The Bordeaux station is our only example of outdoor construction, and is an experiment, although our stations generally are built in the spirit of outdoor stations in the sense that the buildings in principle provide only cover and not support for the equipment.

The standardized 125 MW sets have the following characteristics:

Turbines: 125 MW, 3 000 r.p.m.

Steam conditions at stop valve ..	1 800 lb/in², 1 000° F
Reheat	400 lb/in² (approx.), 1 000° F
Exhaust	0·5 lb/in² absolute

Boiler: output 900 000 lb/h.

Operating pressure at the drum ..	2 000 lb/in²
Superheated steam temperature	1 015° F
Reheated steam temperature ..	1 015° F
Feed-water temperature	465° F (approx.)

The net specific consumption of the power station at economic rating is 9 400 B.Th.U./kWh,* and there is one control room for two adjoining sets.

Each boiler is supplied by three feed pumps of 450 000 lb/h capacity each, with two pumps in service and one in reserve; the pumps are driven by 1 973 h.p. electric motors supplied at 5·5 kV. For the new 250 MW turbines the feed pumps for the 1 700 000 lb/h boiler will be driven by an auxiliary turbine attached to the main turbine. This will make it possible to use supplies of 380 and 5 500 volts and so obviate the need for a third voltage.

It is confirmed that our recent 500 MW stations equipped with four 125 MW units, such as Creil or Porcheville, operate with a staffing of 0·4 man/MW. The total staff of these power stations is less than 200, made up as follows:

(a) An operational department (about 100 persons) responsible for the operation of the machines, coal handling, gardening and cleaning.
(b) A maintenance department (about 60 persons) including a methods office (planning, standing orders, stores) and working shifts. This department is responsible for normal plant maintenance, it being understood that periodic maintenance, during heavy maintenance inspections† of the main sets, is carried out with the co-operation of personnel from the firms who built the machines.
(c) A technical department (about 15 persons) responsible for the operation and the maintenance of driving and control apparatus, water treatment, tests and readings required for the economic control of the operation of the station.
(d) An administrative department consisting of a few persons.

The power stations are technically and administratively supervised by a Regional Group for Thermal Production (G.R.P.T.) consisting of about 40 persons. From the administrative aspect the G.R.P.T. deals with the establishment of the rates of pay for personnel, stock accounting, general accounting and operating costs. These operations are all carried out mechanically. There are 10 G.R.P.T. in France, each having 4–6 stations in its area. The trades unions concerned are kept informed regarding these organizations: as an example, Creil power station, commissioned during 1956, has already produced more than 3 × 10⁹ kWh with this type of organization.

Future Developments.—The French nuclear power-station programme at present aims to achieve a generating capacity of 850 MW by 1965 and then to maintain a rate of development allowing for doubling every three years, giving 2 500 MW in 1970 and 8 000 MW in 1975. The peak capacity of the French network will then be about 20 000 MW in 1965, 29 000 MW in 1970 and 40 000 MW in 1975. The nuclear power-station programme will not modify appreciably the use of conventional

* In December, 1957, the Porcheville power station operated with an average net specific consumption of 9 800 B.Th.U./kWh.
† Boilers every 18 months; sets every three years in principle.

thermal stations recently commissioned or at present under construction during the first ten years of their life—say up to 1970—and thus the economics of these power stations, as described, also remain much the same. It is certain that in a more distant future, the implementation of large nuclear programmes will modify the use and therefore the economics or even the design of conventional thermal stations. These problems will therefore have to be re-examined for the conventional thermal stations to be built—and British experience on units operating on two shifts will then be very useful.

Measures have already been taken to make it possible to shut down 125 MW reheat sets in the evening and restart them in the morning under the best possible conditions of safety by the use, for instance, of a turbine by-pass. The problem will be different from, and not so acute as, that in Great Britain, because we have large-storage and small-storage hydro-electric stations whose aggregate capacity already exceeds 5 000 MW. There is, moreover, the development of gas-turbine peak power stations and an electricity tariff policy which tends to reduce the peak load on the network.

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DESIGN AND APPLICATION OF LARGE SOLID-ROTOR ASYNCHRONOUS GENERATORS

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SUMMARY

Problems in connection with the design and operation of turbo-type generators in certain special cases have been such as to indicate that the characteristics of the asynchronous generator are worthy of consideration. Such generators are not new, but relatively few have been commissioned and these are of small output. They are virtually induction or asynchronous motors, driven above synchronous speed by a prime mover and drawing their magnetizing current from other synchronous machines on the system or from capacitance available therein. The first part of the paper deals with the general principles of such generators, followed by a consideration of the characteristics and problems involved in their design, including core end heating and rotor surface heating. Consideration of the operation of the asynchronous generator enables an estimate to be made of the probable limits of asynchronous heating resulting from loss of excitation on synchronous generators, of the use of asynchronous generators as shunt reactors and their use on the high-speed lines of cross-compounded units.

(1) INTRODUCTION

The asynchronous generator has not been extensively used for power generation, although its characteristics and application have been reviewed in a number of publications[1] and textbooks. Such a generator is characterized by the absence of an exciting winding on the rotor, the excitation being provided by the system to which it is connected. It has been employed to utilize small sources of energy, for example small rivers where generators can be driven by a turbine having a constant gate opening. When water is plentiful an appropriate amount of power will be generated and when water is low the plant will run light. The systems to which such generators are connected must be relatively large, but as voltage regulation is carried out on the synchronous machines, the amount of control and regulating gear on the asynchronous machines is small and makes them most suitable for non-attended stations. The asynchronous generator has also been applied to industrial plant where process energy is available as a by-product.[2]

The present study was made with the object of summarizing the problems associated with the design of asynchronous generators and of considering their application as shunt reactors or as one component of a cross-compound unit. The absence of an exciting winding on the rotor creates a leakage field problem around the stator end windings, leading to stator-core end heating similar to that experienced on a synchronous generator at a leading power factor when the excitation is weak, and also when excitation has been lost. The factors affecting core end heating have been investigated.

The mechanical design of an asynchronous generator of large output necessitates a solid rotor forging, and as the eddy-current losses induced in the rotor surface may become large, their causes have been reviewed.

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(2) ASYNCHRONOUS OPERATION

(2.1) General Theory

It is well known that an asynchronous motor, if driven above synchronous speed, will feed power back into the system, the power bearing a definite relationship to the slip—a reflection of the conditions obtaining when operating as a motor. The simple circle diagram illustrating the operation of such a machine both as a motor and a generator is shown in Fig. 1. Briefly A′

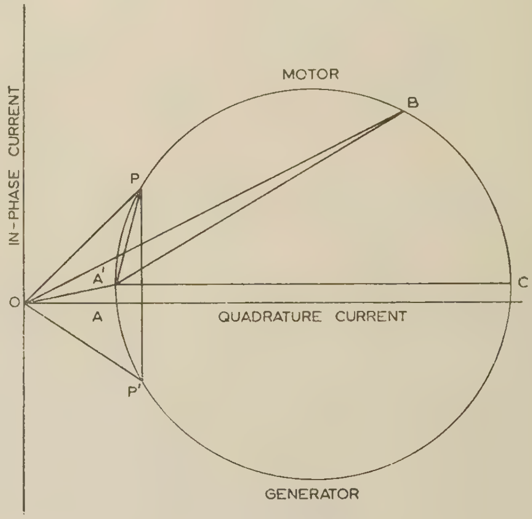

Fig. 1.—Simple circle diagram for asynchronous machine.

is the no-load operating point, and the current vector OA′ has two components, a reactive component OA which produces the flux and a power component A′A which is in phase with the supply voltage and is proportional to the no-load losses. The point B is determined from the short-circuit input at full voltage, and the locus of the current vector OP lies round a circle through A′PB with A′C as a diameter.

The load-current vector OP has two components, OA′ and A′P, the latter balancing the ampere-turns on the rotor. The rotor conductors move at slip frequency with respect to the stator flux, and the voltage thus induced in the rotor conductors causes current to flow in the rotor circuit. As the load is reduced so also are the slip, the induced voltage and the current vector A′P, until at synchronous speed there is no rotor current. If the power flow is reversed and the motor is driven above synchronous speed, current again flows in the rotor conductors but in the reverse sense, and the current locus moves round the locus of the diagram as shown by OP′.

It will be seen that when generating power the machine will still be taking its reactive magnetizing current from the system as when running as a motor, so that the system to which it is connected must be able to supply a reactive component of current.

It will also be evident that such magnetizing currents may be drawn conveniently from a system containing capacitance such as a long transmission line or cable system.

(2.2) Self-Excitation

If a capacitance is connected across the terminals of an asynchronous motor and the latter is run to speed, it will generate a voltage depending upon the speed of the motor and the magnitude of the capacitance. This is illustrated in Fig. 2,

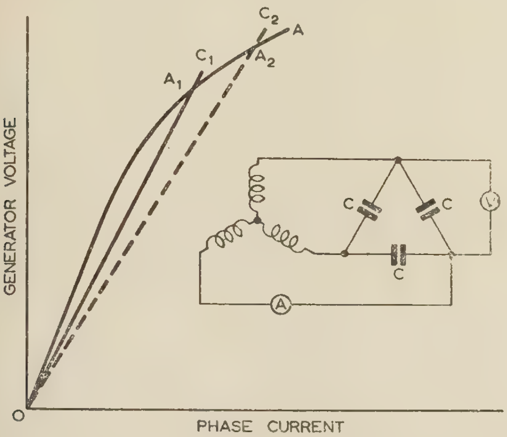

Fig. 2.—Magnetization and capacitance characteristics.

which shows the open-circuit excitation characteristic of an asynchronous motor, together with a capacitance characteristic. The voltage generated will be that corresponding to the intersection of the capacitance characteristic with the open-circuit characteristic, i.e. OA_1. If the capacitance is increased a new capacitance characteristic will be obtained and the voltage generated will be OA_2. The generator would have a drooping load characteristic, but it would be possible to obtain a compounding effect by the insertion of additional series capacitance.

Such a generator would be capable of supplying power through a system containing capacitance, but the voltage and frequency would depend on the load; this condition of operation, which is not envisaged in the present paper, would require study where there existed a possibility of an asynchronous generator together with a portion of the network becoming isolated from the synchronous network to which it was connected.

(2.3) Characteristics of a Solid Forging

The conventional asynchronous motor has a laminated rotor with slots round the periphery carrying the rotor winding, and a similar construction is adopted for relatively small asynchronous generators, the winding being in the form of a squirrel cage. The paper is concerned with generators of large output at high rotational speed where it is essential to utilize a solid rotor forging for mechanical reasons. Clearly, secondary currents will circulate in the rotor surface of a solid rotor and give rise to torque, but in view of the resistance offered to the flow of such currents, consideration must be given to the amount of copper which can be incorporated in the form of a damping or squirrel-cage winding. Various methods of calculation may be applied to determine the characteristics of solid rotors, and while the curves of Fig. 3 were determined for a relatively small machine,[3] similar effects will be observed on large units. Curve A represents the characteristic of a normal asynchronous motor, curve B that for a motor having a solid mild-steel rotor, and curve C that for a motor having a solid mild-steel rotor with the addition of copper damping bars. The latter curve shows

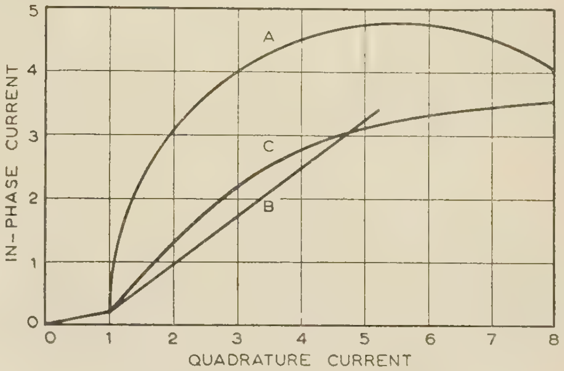

Fig. 3.—Characteristics of various types of rotor.
A. Normal induction motor.
B. Solid mild-steel rotor.
C. Mild-steel rotor with copper damping bars.

that the addition of the damping bars modifies the characteristic obtained with a solid-steel rotor so that it approaches that of a normal laminated rotor with a full squirrel-cage winding.

(2.4) Power Factor

The power factor of an asynchronous machine is inherently low, and while in certain circumstances such as in a shunt reactor this may be turned to advantage, the problem with asynchronous generators in general is to keep the power factor high. The curve of Fig. 4, which represents the manner in which the power

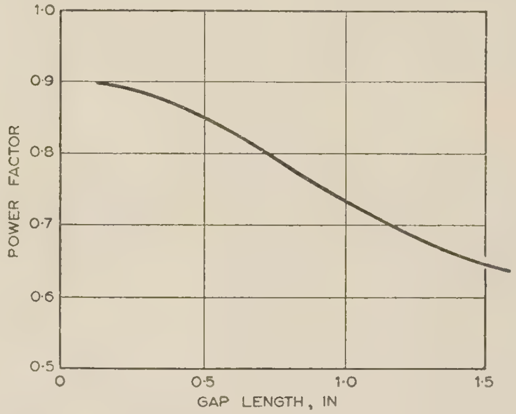

Fig. 4.—Variation of power factor with gap length.

factor may be expected to vary with length of air-gap, has been drawn for a large asynchronous generator whose rotor diameter may be of the order of 36 in. It will be clear that for economy in magnetization the gap must be kept to a minimum. In designing a generator with a small gap, care must be taken to avoid overheating of the rotor surface due to eddy currents set up by irregularities in the stator flux and the ampere-turn distribution. These problems are dealt with in more detail later.

In most large generators the air-gap forms part of the ventilation circuit, and much care is taken to keep the average temperature of the cooling medium round the rotor surface as low as possible. The restriction in gas flow by the use of a small gap therefore introduces a real problem in ventilation.

(2.5) Switching

The asynchronous generator does not carry an exciting winding, and it is put on to the bars of a system by running up to normal speed and switching in without any need for close synchronizing as in the case of a synchronous machine. There is,

however, the initial current rush associated with the establishment of conditions in the magnetic circuit similar to those which occur when switching-in a transformer. Such switching transients can be reduced by the insertion of a reactor during the starting period, but as this would reduce the maximum torque developed, it is essential to short-circuit the reactor before loading up the generator.

(2.6) Fault Current

An advantage of the asynchronous generator is that under fault conditions the fault current decays rapidly and is negligible after the first one or two cycles. The excitation is derived from the system to which the machine is connected, and under 3-phase fault conditions the system voltage disappears. There are, however, at the time of fault, flux linkages associated with the rotor circuit, and as these cannot change instantaneously, rotor and stator currents will flow in such a manner as to maintain the flux linkages. This condition is identical with that associated with the sub-transient reactances in a synchronous generator and for which the time-constant will be similar, i.e. about 0·03 sec. While the asynchronous generator does not impose a duty on the circuit-breakers, the high initial fault current must be considered when designing the protective-gear system.

(2.7) Tests on 2500 kW Generator

Some time ago, tests were carried out on a 2500 kW generator to investigate asynchronous operation. The generator was operated synchronously at a predetermined steady load and the exciter field circuit was then broken; readings were taken of the slip and stator current, following which the main field circuit was broken and readings again taken. On restoration of the original excitation conditions the generator pulled into step without difficulty. These tests were repeated at various loads, and Fig. 5 shows the relationship between load and slip. It will

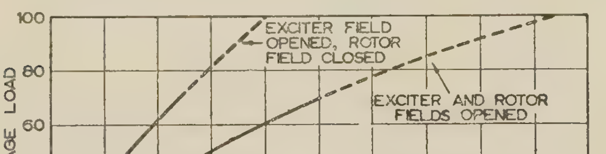

Fig. 5.—Asynchronous operation of 2500 kW generator.

be seen that the slip is a minimum with the main field circuit closed, this being due to the effect of the rotor winding. The amount of secondary loss in the rotor can be estimated from the load and the slip at that load, and with the main field-circuit closed the rotor loss at full load is of the same order as the excitation normally required for rated load. When operating asynchronously it was found that swinging of the stator current occurred owing to the non-uniform reluctance of the magnetic rotor, which was slipping with respect to the stator field, but such surging would not be expected with a solid rotor or one with uniformly distributed damping circuits.

These tests appear to demonstrate that without excitation the losses in the rotor are of the same order as those obtaining when fully excited at rated load. An examination of the available evidence over many years has indicated that generators have been known to lose excitation, in some cases owing to a failure of the exciter field, while in other cases the rotor field switch has been opened. As far as the author has been able to ascertain, there have been no reports of overheating or damage to the rotor from such operation. Nevertheless, as shown in Section 3.6, overheating of the stator may occur, and loss of excitation introduces a problem of protection. It is considered that loss-of-excitation alarms should be fitted wherever possible.

(3) STATOR CORE END HEATING

(3.1) Nature of End Leakage Flux Field

Many studies have been made of the end leakage flux field associated with generator stator windings in connection with end leakage reactance and also the stray losses associated with various end structures and materials.[4,5] It is recognized that these end leakage fluxes are not simple, and search coils positioned in and round the end windings of generators have demonstrated the existence of radial, axial and tangential components of flux. In the following analysis the tangential component has been neglected, and in the leakage flux fields shown in Figs. 6–9 only simple flux plotting has been employed. The analysis may not be precise, but it is believed to be of sufficient accuracy to illustrate the manner in which end-iron heating is affected by the variables of power factor, excitation, short-circuit ratio and material of the rotor end bell.

The end leakage flux field has two components, one being the end leakage on the assumption that the magnetic circuit is energized from the rotor only; this component is related to the gap flux density and is substantially constant. The second component is the end leakage on the assumption that the magnetic circuit is energized from the stator only; this depends in magnitude on the stator current. These components can be called the 'rotor winding field' and 'stator winding field' respectively. The resultant field, i.e. that which causes core end heating, depends on the magnitudes and phases of the two components. It will be shown later that at leading power factor the resultant tends to become large, while at lagging power factor the components tend to be in opposition and the resultant thus becomes small. Asynchronous operation with a zero rotor component gives rise to heating dependent on the stator component only.

That this end leakage flux field can cause overheating of the core end sections of generators has been recognized for some time, and curves showing the manner in which the intensity of heating varies with power factor have been published in the American Press.[6] The extent of the heating has in certain cases imposed a limitation on loading. It should be recorded that in all probability the machines on which this information was obtained have a relatively high short-circuit ratio, i.e. about 0·8, and have magnetic rotor end bells.

(3.2) Estimation of Loss Intensity

The following simple calculation relating to a 3000 r.p.m. generator rated at 60 MW, 0·8 power factor, 0·55 short-circuit ratio, will serve to illustrate in greater detail the method of estimating the intensity of core end loss.

Consider first the end leakage flux field produced by the rotor excitation, neglecting the effect of the stator winding, the conditions being those which normally obtain under open-circuit conditions. A typical leakage flux field for this condition is shown in Fig. 6, in which are indicated the estimated values of flux intensity entering the end packet of the stator. In view of the variation of these densities it is necessary for the purpose of comparison to consider an average condition, and this has been taken as that relating to the mid-tooth position, the length of the associated leakage flux path being shown dotted. If l_g is the length of the air-gap, and l_e that of the end leakage flux path, and B_g the flux density in the air-gap, the densities at various

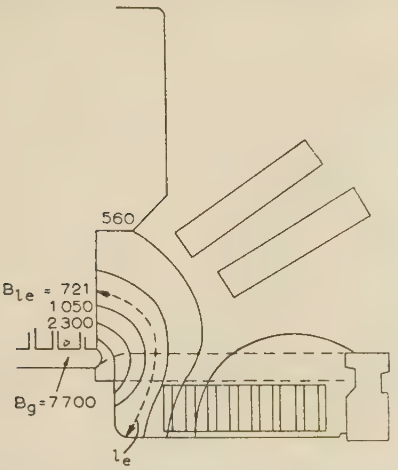

Fig. 6.—Rotor winding field: non-magnetic end bells.
Power factor, 0·8. Short-circuit ratio, 0·55.

positions can be readily determined from the ratio of l_g and l_e. Typical figures for a generator having a short-circuit ratio of 0·55 are l_e 16 in, l_g 1·5 in, $B_g = 7\,700$ gauss, from which the mid-tooth density becomes 721 gauss.

Consider now the condition due to the stator ampere-turns only. The ampere-turns expended on the stator air-gap are 46 800, and the short-circuit ampere-turns, neglecting saturation, would thus be 46 800/0·55 = 85 200. Assuming a reactance of 15%, the ampere-turns expended on the air-gap would be 15% of 46 800, or 7 000, and the stator ampere-turns would thus be 85 200 − 7 000, or 78 200. If these ampere-turns are now considered to be the only exciting force in the end region, the end leakage flux field can be plotted and takes the form shown in Fig. 7. It will be seen that the mid-tooth flux path shown dotted is similar to that relating to the rotor-winding field in Fig. 6. This means that the mid-tooth density with only the stator excited would be 721 × 78 200/46 800, or 1 205 gauss.

When the generator is operating under rated-load conditions the resultant leakage flux in the end windings depends upon the

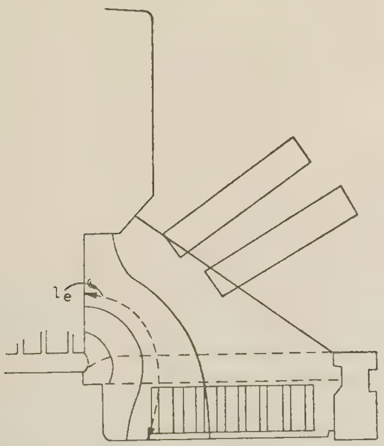

Fig. 7.—Stator winding field: non-magnetic end bells.
Power factor, 0·8. Short-circuit ratio, 0·55.

power factor and magnitude of the load, and these two fluxes must be added vectorially to obtain the resultant flux. The stator ampere-turn diagram, with the associated rotor cross-section to illustrate the relative positions of the rotor poles and the centre-line of the stator and rotor m.m.f.'s, at rated load and

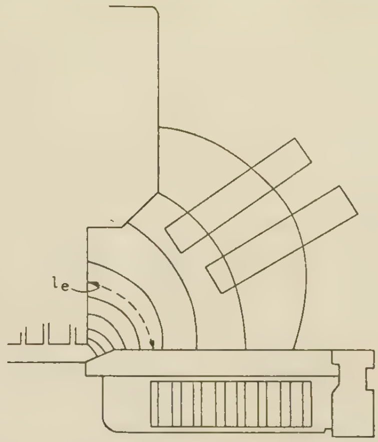

Fig. 8.—Rotor winding field: magnetic end bells.
Power factor, 0·8. Short-circuit ratio, 0·55.

0·8 power factor, is shown in Fig. 10; and the two components of end leakage flux are superimposed on the ampere-turn diagram to illustrate their vectorial relationship. The resultant end leakage flux density AB is determined by setting off OA equal to the rotor-winding field of 721 gauss, and OB the stator-winding field of 1 205 gauss. As the power factor of the load is varied with constant apparent power, it will be seen that OB forms a radius of a circle with centre O. The loss in the stator core-end section due to the flux entering normal to the laminations is probably proportional to the square of the flux density, and by determining AB for various power factors with constant stator current, a power-factor heating characteristic, shown in Fig. 11, can be derived.

(3.3) Effect of Magnetic End Bells

The end leakage flux fields associated with magnetic end bells are illustrated in Figs. 8 and 9, and comparison with similar fields with non-magnetic end bells, Figs. 6 and 7, shows that there is an appreciable increase in the intensity of flux entering

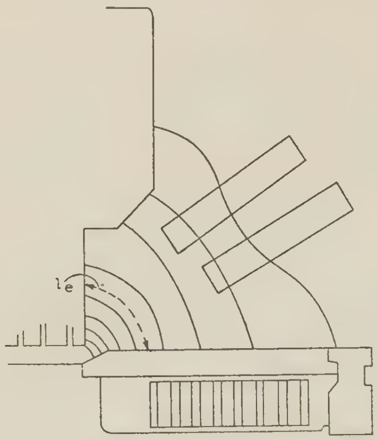

Fig. 9.—Stator winding field: magnetic end bells.
Power factor, 0·8. Short-circuit ratio, 0·55.

the stator core end, particularly in the region round the bore of the stator. In the machine under consideration the path l_e is reduced from 16 in to about 8½ in, the end leakage flux density being increased in proportion. The resulting end heating curves have been plotted in Fig. 11, and that for the magnetic end bells

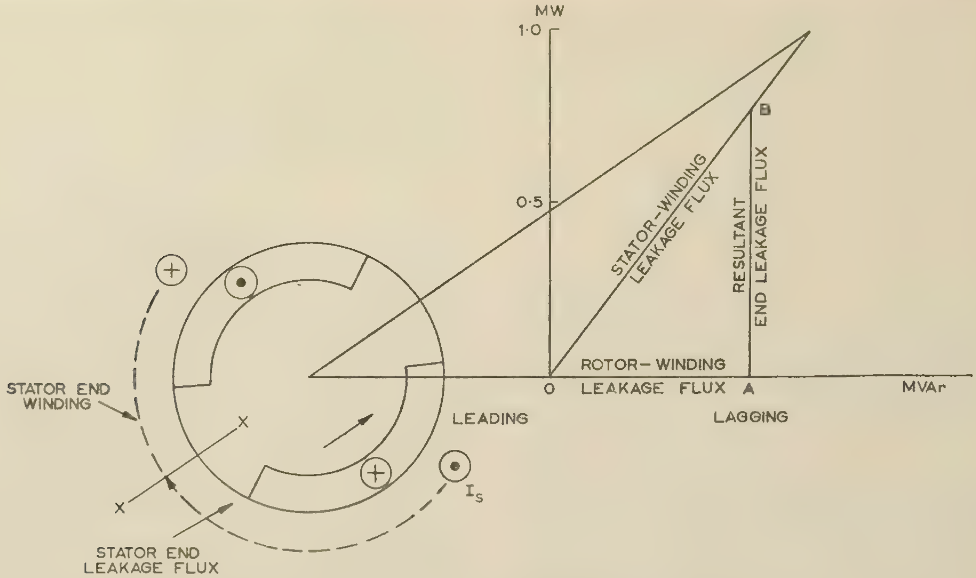

Fig. 10.—Determination of resultant end leakage field.

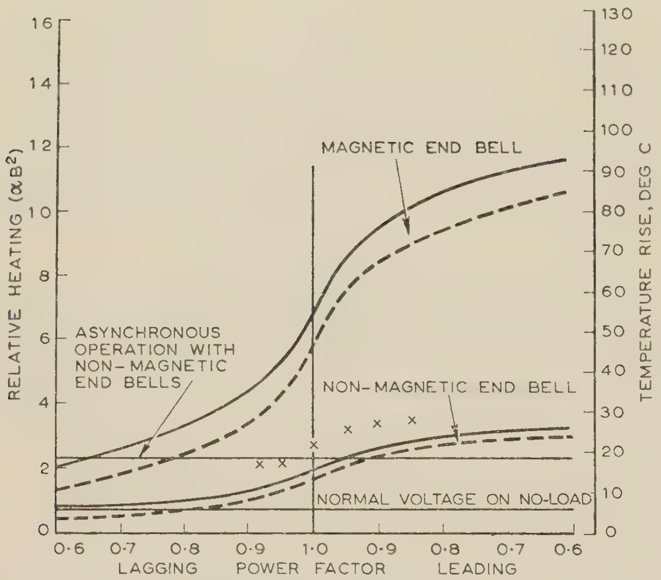

Fig. 11.—Variation of end-iron heating with power factor.
—— Short-circuit ratio, 0·55.
– – – Short-circuit ratio, 1·0.
× Test points measured on 60 MVA generator with non-magnetic end bells.

shows that there is a marked increase in loss intensity compared with the figures obtained with non-magnetic end bells.

(3.4) Effect of Short-Circuit Ratio

The short-circuit ratio is a measure of the relationship between the ampere-turns expended over the air-gap and those due to the stator winding, and if we assume that the generator under consideration is designed for a short-circuit ratio of unity at 0·8 power factor, we know that the rotor excitation ampere-turns remain unchanged for the same temperature rise; the air-gap will be increased from $1\frac{1}{2}$ in to $2\frac{1}{8}$ in, as shown in Fig. 12, and the stator ampere-turns reduced from 78 200 to 59 000. The increase in gap length will increase l_e by about $\frac{3}{4}$ in to $16\frac{3}{4}$ in, and the new value of mid-tooth density corresponding to the open-circuit

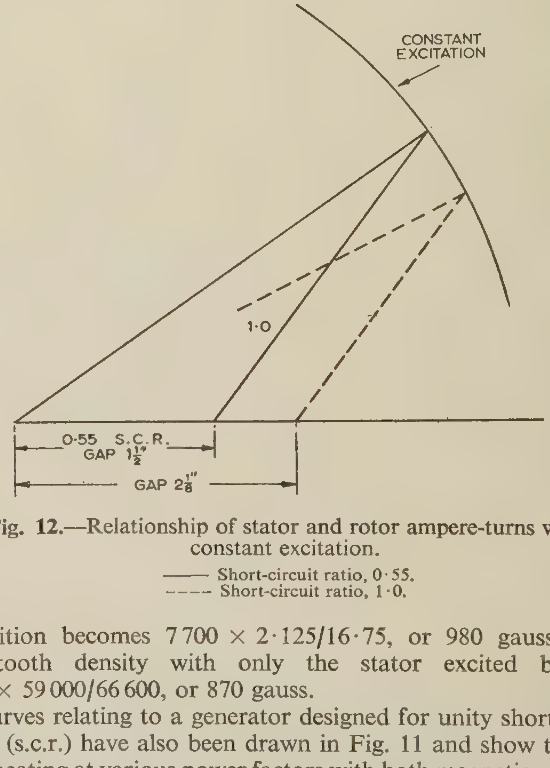

Fig. 12.—Relationship of stator and rotor ampere-turns with constant excitation.
—— Short-circuit ratio, 0·55.
---- Short-circuit ratio, 1·0.

condition becomes $7700 \times 2\cdot125/16\cdot75$, or 980 gauss. The mid-tooth density with only the stator excited becomes $980 \times 59\,000/66\,600$, or 870 gauss.

Curves relating to a generator designed for unity short-circuit ratio (s.c.r.) have also been drawn in Fig. 11 and show the core end heating at various power factors with both magnetic and non-magnetic end bells. The effect of s.c.r. is small, similar heating being expected in machines designed for both 0·55 and unity s.c.r. It should be remembered, however, that the stator ampere-turn loading has been reduced to assist in raising the s.c.r. to unity, so that on a basis of similar stator rating the effect of a high s.c.r. is to increase the intensity of core end heating.

(3.5) Core End Heating Limit

The manner in which core end heating varies with load and power factor is illustrated in Fig. 13, but such a set of curves does not provide a ready guide to an operator, and any core end heating limits must be expressed more conveniently. Assuming, for

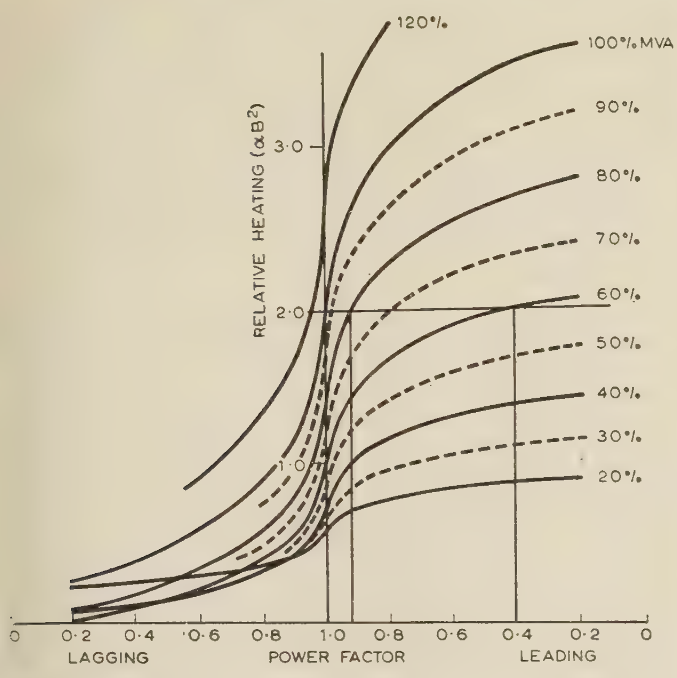

Fig. 13.—Set of end-iron heating curves: non-magnetic end bells.

example, that tests have shown that a heating limit is reached when the loss (proportional to B^2) is $2\cdot0$; it will be seen that this level of heating is attained at, for example, 60% MVA at 0·4 p.f. leading or 80% MVA at 0·92 p.f. leading. Such points can be plotted on the conventional stability loading chart shown in Fig. 14, where the curve AA has been plotted corresponding to a

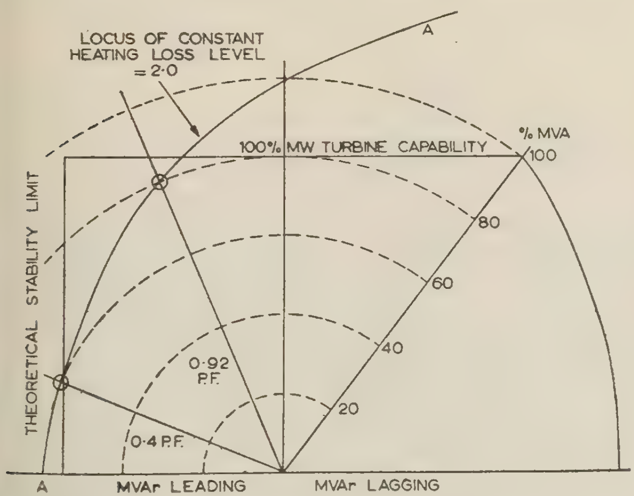

Fig. 14.—End-iron heating limit superimposed on stability diagram.

loss level of 2·0. The curves in Fig. 13 relate to a generator having non-magnetic end bells, for which the actual permissible loss level will be greater than 2·0 and in all probability will fall outside the stability diagram. The use of magnetic end bells may cause greater heating, and the loss level may well restrict the loading at leading power factor.

(3.6) Comparison of Short-Circuit and Zero-Power-Factor Heating

No-load tests carried out on generators at rated current both at zero power factor and under steady 3-phase short-circuit conditions have indicated that the core end heating tends to be greater under short-circuit conditions than at zero power factor. Consideration of the flux diagram in Fig. 10 shows that at zero power factor lagging the resultant core end leakage flux is given by the difference between OB and OA, whereas under short-circuit conditions it is nearly proportional to OB. The core end conditions with a zero-power-factor test are therefore somewhat optimistic in regard to the core end temperature.

(3.7) Asynchronous Core-End Heating

When operating asynchronously the end leakage flux is that due to the stator current only, and is thus proportional to the vector OB in Fig. 10; the intensity of heating will be similar to that obtained under short-circuit conditions.

When a synchronous generator loses excitation, however, the stator current becomes larger than normal owing to the out-of-phase component of current and may approach 200% of rated current. The end leakage field therefore becomes correspondingly severe and may cause overheating of the core end plate and the end sections of the stator core. The end sections of a stator core are usually designed to have a relatively large cooling surface per unit volume, and the thermal time-constant may be expected to be appreciably shorter than that of the main body of the generator. Heating tests at leading power factors have indicated that the temperature changes due to core end heating occupy about 20 min. This means that, in the event of a generator losing excitation, unloading should commence as soon as possible to reduce the stator current to a value not exceeding full load current. During this unloading period, examination of the excitation circuit will show whether the fault can be rectified and the excitation restored, or whether the loss of excitation is likely to be prolonged, in which case unloading could continue and the set be taken out of commission.

(3.8) Predetermination of Temperature Rise

The calculated end iron heating curves in Figs. 11 and 13 have been shown in terms of relative heating, and no attempt has been made to express heating in terms of specific temperatures. The actual loss which would result from a given flux density depends upon the metallic part concerned, whether solid or laminated, and its electrical and magnetic properties. The actual temperature of the part concerned would depend on its cooling surface and the velocity and mass-flow of the cooling gas. Manufacturers in general will have details of temperature rises measured at the ends of generators they have tested under various loading conditions, and will be able to determine an empirical factor to insert in their loss calculations.

Information regarding core end heating published in 1929[4] showed that the core end temperature at constant apparent power fell from 70° C underexcited to 20° C overexcited. More recently, core end heating curves have been published[6] which are very similar in shape to those calculated and shown in Figs. 11 and 13. The hottest core end temperature fell from about 75° C at 0·8 p.f. leading to about 35° C at 0·8 p.f. lagging. The latter figures were obtained on a large generator rated at 112·5 MVA and operating with a hydrogen pressure of $\frac{1}{2}$ lb/in^2 (gauge), but again no design details were given which would correlate the results with the specific rating of the generator.

Core end heating tests have been carried out on a 3000 r.p.m. hydrogen-cooled generator rated at 60 MW 0·8 p.f. 0·55 s.c.r., operating at a gas pressure of $\frac{1}{2}$ lb/in^2 (gauge) and having a total stator ampere-turn rating very similar to the figures taken for the calculation given in Section 3.2. The rotor was fitted with non-magnetic end bells, and the core end plate was of magnetic cast iron. Tests were carried out at a constant loading of 60 MVA at power factors between 0·9 lagging and 0·85 leading. The

curve of temperature rise of the hottest core end thermocouple is superimposed on Fig. 11, and it will be seen that, while the temperature level is low, the shape of the curve is very similar to that predicted.

(4) ROTOR SURFACE HEATING

Reference has already been made to the secondary currents which flow in the rotor windings and body, and which vary in proportion to the power generated by the machine, but there are a number of other factors which give rise to eddy currents in the rotor surface, particularly when a solid rotor is employed, and may result in serious overheating.

(4.1) Flux Tufting

As is well known, one of the serious disadvantages of a short air-gap with respect to the slot width and slot pitch is that pole-face losses are induced in the rotor surface due to the tufting of the flux under each of the stator teeth. This tufting can produce a high eddy-current loss in the rotor surface which appears as the machine is excited and is normally measured and included with the open-circuit iron loss. The factors affecting this loss have been the subject of a number of papers, and it is calculable with a fair degree of accuracy.

(4.2) Phase Band Distribution

There is also a rotor surface loss resulting from the spatial distribution of the stator phase bands. The stator m.m.f. wave of a 3-phase winding varies between two limiting waveshapes, and the harmonics corresponding to these waveshapes induce eddy currents in the rotor surface when the stator windings are carrying current. It is, for example, fairly well recognized that with a winding chorded 80% the harmonics are a minimum, resulting in the least loss in the rotor surface. Again, as the air-gap is reduced in length the rotor surface losses due to this cause increase, but a certain number of machines have been built and tested in which the rotor surface losses on load appeared to be appreciably greater than could be accounted for by the distribution of the stator phase bands.

(4.3) Slot Ampere-Turn Concentration

Investigations were made into the variation of flux density round the bore of an energized stator with a dummy rotor in position. It was found that the ampere-turn concentrations in individual slots resulted in appreciable flux variations, and by varying the air-gap the relationship between the gap and slot pitch was determined. The experiments were repeated with various stators having different winding and slot arrangements.

The curves shown in Fig. 15 illustrate the relationship between the number of slots and the length of the air-gap. These curves were drawn on the assumption that the dimensions of a generator remain constant for a given output and that the number of stator slots can be varied over a wide range. It will be seen that, as the slot pitch becomes large, the loss increases rapidly. It is therefore important to appreciate the need for care in the selection of slots; for example, there is sometimes a choice of building a machine with either 72 or 36 slots, and while from a manufacturing point of view the smaller number will probably result in a lower manufacturing cost, care must be taken to investigate the relationship between the stator slot pitch and the length of the air-gap before making a final decision to adopt the smaller number.

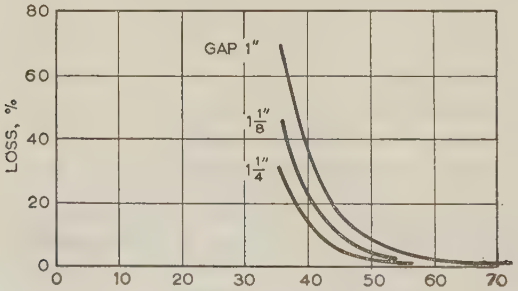

Fig. 15.—Rotor surface loss due to stator-slot ampere-turn concentration.

(5) APPLICATIONS OF THE ASYNCHRONOUS GENERATOR

(5.1) Shunt Reactor

The amount of leading current necessary to charge transmission lines, particularly where they are being installed at a greater rate than generating plant, introduces serious difficulties in connection with the stability of the system, and consideration has had to be given to the installation of shunt reactors to offset the line charging. It would therefore seem that, in circumstances such as these, it should be possible to make use of the characteristics of the asynchronous generator, which relies upon the capacitance in an overhead line or other synchronous plant to provide its excitation. In addition to performing the duty of a shunt reactor, the generator would, of course, provide useful power to the system.

Under conditions of light load the corrective effect of an asynchronous generator would be that corresponding to the no-load magnetizing current. As the load built up on this generator, the out-of-phase component would increase as demonstrated in Section 2.1, but as the increase would be supplied by the synchronous machines, it would merely serve to increase their stability. The control and characteristics of an asynchronous generator were explained in Section 2.1.

(5.2) Cross-Compound Units

(5.2.1) Turbine Arrangement.

In view of the increasing demand for large units, power engineers are turning their attention to the application of the cross-compound unit; in certain cases this may consist of two high-speed lines, and in others a high-speed and a low-speed line. Where the latter scheme is envisaged it would appear that the asynchronous generator may have a useful application on the high-speed line. With such a scheme, the high-pressure high-temperature steam is fed into the high-pressure turbine on the high-speed line, which runs at the maximum permissible speed. In this manner advantage is taken of the small-diameter turbine spindle, which is advantageous from the point of view of thermal mass and blade annulus. The high-pressure line feeds into the low-pressure line, and in general the latter runs at half the speed of the high-speed line, i.e. the generator has four poles instead of two. In this case it becomes possible to provide an exhaust annulus capable of handling the large volume of low-pressure steam.

In large units an intermediate-pressure section is provided between the high-pressure and low-pressure turbines, which can be incorporated in the high-speed or low-speed line or in some cases split, part of the section being on each line. Another factor controlling the arrangement of the turbine cylinders is the overall length of the unit, and as far as possible the units are proportioned to occupy a similar length. The arrangement of the turbine does not fall within the scope of this paper, but, broadly speaking, the powers available from the high-speed and low-speed lines are similar, although a reasonable variation is feasible in either direction.

(5.2.2) **Rating of Units.**

As already explained, the asynchronous generator draws its excitation from other units, so that when it is used it is convenient to regard a cross-compound unit as an entity; i.e. the low-speed generator must provide the excitation for the high-speed generator. To illustrate the argument a hypothetical case of a 200 MW cross-compound unit, rated at 0·85 p.f., is considered. This is split into two equal lines each rated at 100 MW. It is also assumed that the high-speed unit containing the asynchronous generator will deliver its output at a power factor of 0·9 leading. The unit as a whole must deliver 200 MW and 123 MVAr, and since the only synchronous unit is on the low-speed line this generator must be designed for an output of 100 MW and 123 MVAr. In addition, however, the low-speed generator must provide the magnetizing power for the high-speed generator, which, at a rated power factor of 0·9, amounts to 48·6 MVAr; i.e. the low-speed generator must be rated at 100 MW and 171·6 MVAr, or a rated power factor of about 0·5. Fig. 16 illustrates the foregoing steps which have to be taken to determine the rating of the low-speed generator.

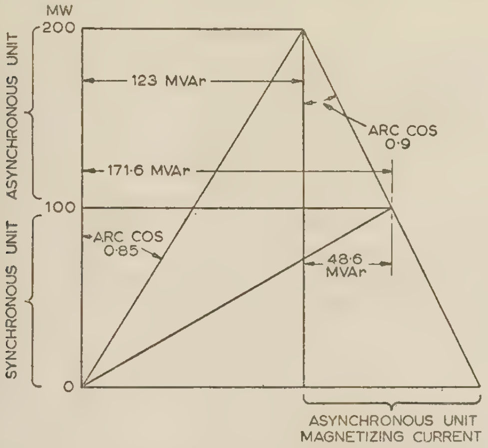

Fig. 16.—Determination of rating of synchronous unit in cross-compound arrangement.

(5.2.3) **Effect of System Power Factor.**

The foregoing arguments have been applied to a unit designed to generate at an overall power factor of 0·85, but if, for example, the loading conditions are such that the rated power factor of the unit becomes unity, the low-speed generator will only have to supply the magnetizing current for the high-speed generator; i.e. its rated power factor will be 0·9 lagging. This is reasonable from the point of view of generator design, and it will thus be apparent that the power factor at which the unit delivers its output has a large effect upon the power factor for which the low-speed generator must be designed. This variation is illustrated in Fig. 17, which shows the relationship between the power factor of the load delivered to the system and the power factor of the low-speed generator for three different ratios of load split, namely 2 : 1, 1 : 1 and 1 : 2. It will be seen that it is advantageous to keep the output of the high-speed unit less than that of the low-speed unit, and that, where the power delivered to the system is at a power factor of unity, the electrical rating of the low-speed generator is not severe.

(5.2.4) **Effect of Short-Circuit Ratio.**

Where generators are intended to supply long overhead-line systems, an immediate problem is that of stability, and, as is well known, it has been customary in America to design generators having a short-circuit ratio of unity. Such generators must be

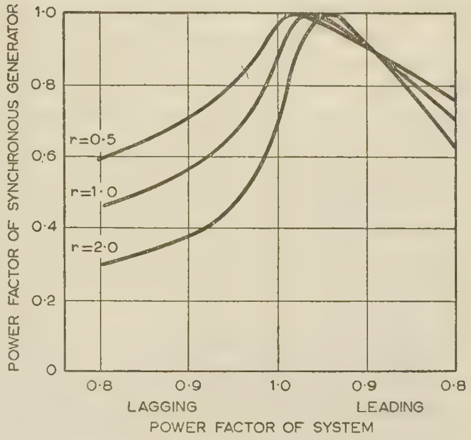

Fig. 17.—Effect of system load power factor on rating of synchronous unit.

$$r = \frac{\text{Output of asynchronous unit}}{\text{Output of synchronous unit}}$$

larger and more expensive than those designed for a lower short-circuit ratio, and the limitations imposed on maximum permissible output by a high short-circuit ratio are such that on modern generators there is an increasing tendency to adopt a lower short-circuit ratio. The type of cross-compound unit described above, instead of having to provide line-charging capacity, in effect utilizes it as a means of excitation and thus puts the line capacitance to a useful purpose; the difficulty of providing generators having a high short-circuit ratio is thus minimized. The characteristic of an asynchronous generator is such that the magnetization required at various loads changes relatively slowly and thus more closely compensates for line-charging capacity over a wide range of load.

(5.2.5) **Features of High-Speed Unit.**

The construction of the high-speed line is more simple and robust than that of the equivalent synchronous generator. The rotor, which would normally have to be of a highly stressed alloy steel, may be replaced by a lower-grade forging, and the use of a highly stressed end bell is avoided. The rotor body end need not suffer the rapid reduction in section normally required to accommodate the rotor end windings, and could be tapered steadily from the body end to give a stiff shaft. The advantages of shaft end stiffness, and consequently high critical speed, are obvious. The absence of an excitation winding avoids the use of slip rings and the associated problem of current collection at high surface speeds.

(5.2.6) **Features of Low-Speed Unit.**

The normal direct-current excitation of the two units is concentrated into the single low-speed unit, the excitation on that unit being correspondingly increased. Fundamentally the low-speed design of generator can accommodate adequate copper on the rotor without attaining excessive mechanical stresses, and this is particularly evident with the direct-cooled design. The additional exciting current leads to a problem of current collection, but here again the stiffness of the shaft end, the area of slip-ring surface which can be provided and its relatively low surface speed render the current collection problem less difficult. The mechanical stresses in the rotor end bells and in the rotor body are moderate, and for the latter a low grade of steel is probably permissible.

(5.2.7) **Cost and Efficiency.**

It is evident that the frame size and efficiency of the proposed units on the high-speed and low-speed lines will not be identical

with what might be regarded as the normal synchronous arrangement of units, and the overall economic position can best be illustrated by comparing details of the units required to supply a system load of 200 MW at 0·9 p.f. leading. It appears to be most convenient to make a direct comparison between the units on the high-speed lines and those on the low-speed lines.

Of first consideration is the rating of the units in relation to the reactive power requirements. The total reactive demand corresponding to a power factor of 0·9 is 96·82 MVAr leading. A general consideration of the design of the high-speed asynchronous generator indicates that it may be expected to have a rating of 100 MW at a power factor of 0·75 and will therefore absorb a total reactive power of 88·2 MVAr. The net reactive power to be supplied by the synchronous unit will therefore be 8·62 MVAr leading, so that this unit will have a rating of 100 MW at a leading power factor just removed from unity.

The costs of generators, other factors being equal, may be assumed to vary rather less than the ratio of their D^2L products, where D is the outside diameter and L the length of the core. It will be clear that the generator frame size of the high-speed asynchronous unit rated at 0·75 p.f. leading must be larger than that of the equivalent conventional high-speed synchronous generator rated at 0·9 p.f. leading, and the D^2L ratio is approximately 138 : 100. The low-speed generator rated at about unity p.f. will be somewhat smaller than the conventional generator rated at 0·9 p.f. leading, the ratio being approximately 94 : 100. The ratio of the total D^2L's for the two schemes considered is 232 : 200, an increase of 16%.

On the score of efficiency the larger asynchronous unit on the high-speed line must have relatively larger no-load losses, and as the stator current of this unit is some 20% larger than that of the corresponding synchronous unit, the load losses will be larger. There remains the excitation loss, and although the asynchronous unit has no exciting winding, there is a loss in the rotor surface due to the slip-frequency currents; this loss can be minimized by the use of a squirrel-cage winding. On the other hand, the low-speed synchronous unit rated at a power factor of about unity must have an efficiency at least equal to, if not slightly better than, that of the corresponding unit rated at a power factor of 0·9 leading.

On balance, therefore, there is an increase in the first cost based on the D^2L figures and a slight reduction in efficiency of the asynchronous-synchronous scheme compared with the conventional synchronous arrangement. This additional cost and loss are mitigated to some extent by the less costly construction of the rotor of the asynchronous generator, and must also be offset by savings which may accrue from the reduced cost of switchgear and simplicity of control.

(6) CONCLUSIONS

(i) A study of the design problems associated with large asynchronous generators indicates that such machines are practicable. Of particular interest is the problem of stator core end heating, which also has an interesting application to the problem of core end heating at leading power factors on synchronous machines.

(ii) The use of the asynchronous generator as a shunt reactor in conjunction with synchronous plant on large systems may assist by improving system stability.

(iii) The asynchronous generator may have an application in the high-speed line of a cross-compound unit, in particular where the power factor of generation is high. In certain cases it may be considered expedient to install generating plant close to either fuel or water, and to transmit the generated power over relatively large distances to the load centres, which introduces the problem of line charging.

(7) ACKNOWLEDGMENTS

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DISCUSSION BEFORE THE SUPPLY SECTION, 26TH FEBRUARY, 1958

Mr. W. N. Kilner: The temperature-rise figures for the 60 MW generator, shown in Fig. 11, are low, and it might be assumed from this that core end-heating is not a serious problem.

I understand that the stator ampere-turn loading of this generator is small by modern standards, and that generators are now being made where the ampere-turn loading is 80% greater. Unless some special precautions are taken in the design of the more highly rated generators, such as the provision of dampers in the end-leakage field, core temperature rises of the order of 100° C may occur. I presume that the generator referred to in Fig. 11 was not provided with end-field dampers.

The author has shown that, if asynchronous rotors are used, the burden of providing excitation must be transferred to some other part of the generator—the stator—or to the supply system or the rotors of some other synchronous generator. But, can the other components be made to take over the duties which the asynchronous rotor cannot now perform? Yes, says the author, and if you have a power system which operates at 0·9 power factor leading, and you require a 200 MW set and are satisfied that this should be of the cross-compound type, with the loads divided equally between the two lines, and if it is economically sound to run one turbine at half the speed of the other, then you can make the high-speed generator of the asynchronous type, and the overall cost of both generators will be increased by about 16%, and the efficiency will be a little lower. We should like to ease the burden of the synchronous generator rotors, but not, I suggest, at that price.

And here is another difficulty. If the power factor of the load should change from leading to unity power factor, and it is likely to do this in most systems when they are fully loaded, the proposed 200 MW generator set would only be able to deliver less than 25% of its rated load, with corresponding loss of revenue from capital invested in the boilers, turbines and transformers. Full output could, of course, be obtained by installing a large synchronous condenser, but this would be expensive.

It is unlikely that there are many large systems fed by thermal

plant which operate continuously at a power factor of 0·9 leading. I submit that a more normal case on which the economics of the problem should be considered is the hypothetical one referred to in Section 5.2.2. Here the normal system power factor is 0·85 lagging. Conventional synchronous generators designed for 0·85 power factor lagging would be able to deal with full load at any power factor between 0·85 lagging and unity or 0·95 leading.

The MVA rating of the high-speed asynchronous generator is about 6% less than that of the corresponding synchronous generator, but the low-speed generator rating is increased by 70%. The overall increase in MVA is 32%, and I would suggest that the overall increase in cost would be of the same order. This is a conservative estimate, because the asynchronous generator would probably have a larger D^2L than the synchronous generator. This is a factor which must also be considered, having in mind the very real difficulty of transporting the stators of large generator units.

Would the author say why, in his conclusions, he states that the asynchronous generator may have an application in a cross-compound unit, when the paper in general indicates that it is not an economic proposition?

Mr. L. W. James: A similar investigation was carried out some seven years ago, when it was suggested that it might be reasonable to carry a few solid rotors as emergency spares for power plant in this country. Tests were made on a 30 MW generator to check calculations, and loads up to about 12 MW were carried for a few hours without excitation. At such loads the stator windings and core ends appeared to reach limiting temperatures, and this was coupled with adverse voltage effects on the local system.

The author refers to switching-in without close synchronizing, but with the same standard of stator-winding bracing this could equally well be used for wound-rotor machines, the excitation being applied after the machines had been switched on to the system.

The fault currents from asynchronous generators should be taken into account when dealing with the magnetic effects of peak short-circuit currents, and on a large system the short-circuit capacity of switchgear appears to be determined largely by the in-feed to a generator-transformer circuit fault, so that it is unlikely that any major saving could be made in this respect.

Recent assessments of the cross-compound machine for this country indicate that both lines should run at 3000 r.p.m., and there would therefore not appear to be a case for one of these machines being an asynchronous generator.

An assessment of the losses obtained with the combination proposed in Section 5.2.2, which gives a power factor suitable for this country, suggests that the overall efficiency must be reduced by approximately ½%, when compared with two synchronous lines, and this, coupled with the increase in MVA from 236 to 311, would appear to rule it out on economic grounds.

Would not the suggestion that the rotor body ends could be tapered steadily from the body result in a further increase in the end heating and stray losses?

Mr. V. Easton: It appears from Fig. 4 that, to obtain a power factor of 0·9 leading on the asynchronous generator, a gap of about 0·2 in is required; this would give rise to high rotor surface loss in addition to posing serious cooling problems. With a more practical gap length to give a power factor of 0·75 as in Section 5.2.7, the rating of the low-speed line is increased to 238 MVA, so that the asynchronous generator becomes a unit extra to plant capable of supplying more than the specified output. This cannot be economically or technically sound, particularly as there are other more simple methods of compensating leading reactive power which will also cater for changes in system power factor during periods of heavy load or as a future development. The possible application of large units of this type must therefore be very limited.

The subject of stator end heating is involved, and there is no doubt that the over-simplified treatment of the paper has led to some erroneous conclusions. The method is obviously incorrect at low lagging power factors when the indicated heating is less than that on open-circuit. The shape of the predicted curve at higher power factors is similar to that drawn through the test points, but this is a coincidence rather than a true explanation of the phenomenon. The basis of Fig. 11 is so ill-defined that it is difficult to reach any definite conclusion with regard to the effect of higher short-circuit rating, although the heating may be expected to increase slightly for the same unit output. With magnetic end-bells, infinite permeability has been assumed to give a much shorter effective flux path and increased heating in the ratio of 3·6 compared with non-magnetic bells. While this ratio may be correct for some alternators, it is not applicable to others of different construction on which tests have shown negligible difference on open-circuit and at zero power factor with factors of about 1·35 to 1·5 at high leading power factors. Has the author any data relating to variation in heating down the tooth or to substantiate the curves of Fig. 13, wherein the heating varies approximately as the first and not the second power of the specific loading?

Mr. V. J. Vickers: My company has had the opportunity of running an air-cooled 30 MW alternator and a 60 MW hydrogen-cooled alternator asynchronously, and the values of slip obtained with the fields open are shown in Fig. A superimposed on the

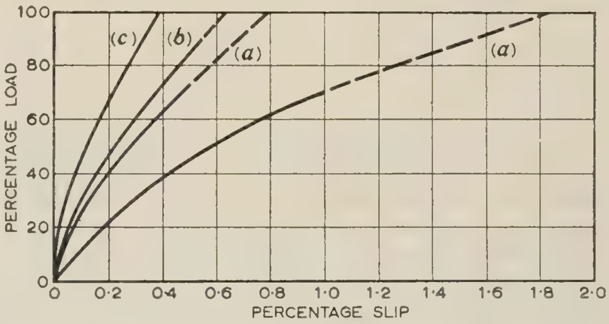

Fig. A.—Asynchronous operation of turbine-type generators.

(a) Curves of Fig. 5.
(b) 30 MW generator with field open.
(c) 60 MW generator with field open.

curves of Fig. 5. The 30 MW machine was run asynchronously for periods up to ¾ hour at a time at 12 MW and with a stator current of about 85% of rated value. Thermocouples in the end-packets of the stator iron showed a rapid rise in temperature from 45° C to 90° C, at which they levelled off after about half an hour. An opportunity was taken later to remove the rotor end-bells and no signs of general or local overheating were found; this, coupled with the fact that colour-change paints which had been applied to the rotor did not show any significant temperature increase, confirmed our view that, when operating asynchronously, the limit in output would be set by the stator rather than the rotor.

The low temperature rises measured on the machine of Fig. 11 may be the result of the relatively low ampere-conductor loading, but I would ask the author whether his detectors were located between the end clamping-plate and the first lamination or inserted in holes drilled in the outer surface of the plate. Further, would the author give his views as to the effect on the temperature rise of the end iron of the substitution of non-magnetic end-plates?

The suggestion is made in Section 5.2 that the asynchronous generator may have an application for the high-speed line of a cross-compound unit where the system power factor is high. While not necessarily agreeing with the economics of such an arrangement, it seems to me to pose a problem in starting. I would ask the author for his proposals for running such a unit up, bearing in mind that in many cases the steam input necessary to run the high-speed line up to rated speed will only give a relatively low speed on the other line.

Dr. P. D. Aylett: The author suggests that an asynchronous generator would impose no fault duty on the circuit-breakers. It is, however, necessary to consider the possibility of closing a breaker onto a fault, when the maximum fault currents, including the sub-transient currents of the asynchronous machines, would be available.

As the system is developing in this country, the asynchronous generator might become an attractive proposition. There is a large h.v. cable network in the London area the capacity of which is bound to increase greatly in the future. Asynchronous generators could be installed in this area to absorb the magnetizing current supplied to the system by the cable capacitance, thus avoiding the expensive alternative of providing shunt reactors.

Unfortunately, we cannot ensure that the asynchronous generator connected close to the cables will be running at the time when it is required to absorb magnetizing current. This is likely to be at night, when generators at the coalfields or in nuclear-energy stations producing the cheapest active power would be operating, and those in the London area would be shut down. Asynchronous generators would thus not solve the problem in the London area.

An important question which influences system designers is that of transient stability. When there is a system fault the synchronous machines will start swinging and may go out of step if the fault is not cleared rapidly. It may be thought that asynchronous machines would have better characteristics in this respect. They cannot, after all, go out of step. However, there is another problem which arises with asynchronous machines.

There will be a pull-out torque just as for an induction motor, and incidentally I would have liked to see some estimate of the torque/slip curve for the hypothetical generator which the author has suggested. Now, a synchronous machine will give the equivalent of about five times its rated torque under transient conditions. To produce the equivalent performance, the asynchronous machine should have a pull-out torque of about five times its rated value, when connected through a step-up transformer onto a strong busbar.

We have recently carried out tests on synchronous generators, and in one case we found that the asynchronous pull-out torque was only about one-and-a-half times rated torque. Further, the asynchronous output depends upon the square of the system voltage, so if the system voltage collapses the asynchronous torque is reduced much more than the synchronous torque. It is unlikely, therefore, that the machine suggested by the author will have any advantage in meeting the problems of transient stability.

It does not look as though asynchronous generators will be very useful to us in this country, but I consider that if synchronous generators can be designed so that they can run asynchronously, not just for a few minutes as at the moment, but for longer periods, it would be a most useful advance in design. I do not think this will prove difficult, since the short-circuit ratios are falling and transient reactances are rising as a natural consequence of the development of large direct-cooled machines. This will mean that the machine will tend to take smaller currents when running asynchronously. If we can get a 0·9 power-factor (lagging) machine which when run asynchronously only takes current at 0·9 leading, it is clear that the only difficulty likely to arise is excessive core-end heating.

Since in the future I consider that our synchronous machines will be required to operate with power factors down to 0·9 leading in some circumstances, the problem of core-end heating must in any case be solved by the designers.

Mr. D. J. Miller: A previous speaker has questioned some of the simplifying assumptions that the author made in arriving at the relative heating curves, but I am wondering whether his fundamental theory is right in this respect. Fig. 7 shows the flux coming from the stator end-core and linking with almost all the field windings. On the other hand, Fig. 10 shows the stator-winding leakage flux opposed, as it were, by the rotor-winding m.m.f. corresponding to normal voltage on open-circuit. Because this stator leakage flux is linking with the whole of the field windings, it ought to be opposed by the whole of the field ampere-turns at that load. Putting it another way, the author is applying the principle of superposition, and neglecting saturation, should consider the two total m.m.f.'s, the one due to all the field ampere-turns being opposed by all the stator ampere-turns, to get the resultant leakage flux.

According to the curves in Fig. 3 it appears that the reactance of the rotor is the trouble rather than the resistance as stated in Section 2.3.

With reference to Section 2.4, could the author give me any indication whether, with the small air-gaps that would be necessary, any trouble would be experienced with unbalanced magnetic pull, particularly as it might affect bearing design?

The fact that the asynchronous generator would not provide a continuous fault current might not be entirely an advantage, since with over-current relays as the basis of most schemes of back-up protection it is necessary to have this current.

The author's paper is very valuable, even if we never have large turbo-type asynchronous generators. Recently we have found that certain Continental manufacturers are prepared to put forward large synchronous condensers to operate with negative excitation. Whether they do so with full knowledge of the core end-heating effect is not certain, but with this method of operation there can be significant savings in the size and cost of the machine which we in Britain cannot afford to neglect.

Mr. B. Adkins: The discovery that it is possible to run a synchronous machine asynchronously with a very small slip and without excessive rotor losses appears to have come as a surprise, but if full use had been made of methods of calculation that have been available now for 28 years, there would have been no occasion for surprise at all. The well-known paper by Park* indicated the method which could be used for calculating the asynchronous characteristics of synchronous machines, and this was amplified in some detail by Linville.† I am sure that it would have been possible, at least approximately, to apply the method to this problem.

I should like the author's opinion on the relative importance of the damper bars and of the solid rotor itself in producing a damping effect in a typical turbine-type machine.

It is interesting that the author has approached the induction motor in the light of his experience on the synchronous machine. That seems to be very logical. The common method of approaching the theory of the induction motor is to start with the transformer and to regard the induction motor as a kind of generalized transformer. It is, however, much more important that the induction motor is a rotating machine in which mechanical power is produced, than that its circuits have something in common with the transformer. The induction motor can be

* *Transactions of the American I.E.E.*, 1929, 48, p. 716.
† *Ibid.*, 1930, 49, p. 531.

considered just as a special case of a synchronous machine—a synchronous machine with no field winding and with the damping system symmetrical on the two axes.

Mr. D. P. Sayers: Having listened to the author and the discussion I am at a loss to understand what useful purpose can be served by an asynchronous generator. The author poses one or two possible applications in most unusual circumstances, but even in such cases I would think that a synchronous generator together with suitable reactors or capacitors would meet the requirements more economically. Can the author say whether the asynchronous machine has any real application in the modern system?

Dr. W. J. Gibbs: I want to criticize the paper from the design point of view. The paper gives the impression that the main difficulty in designing solid-rotor machines lies in dealing with stator-core end-heating. Although this is important, I should have thought it could be treated in the usual way by experimental work with damping circuits. I am sure the author will find that his real difficulties will be associated with other topics he mentions in his paper, topics over which he skates very lightly indeed.

Mr. V. Ahmad (*communicated*): In order to improve the power factor, an asynchronous generator must be designed with as small an air-gap as possible. The author concerns himself with the development of high-speed asynchronous generators of large output with solid rotors, but I take it that he considers it rather unavoidable to use either damper bars without end rings or a squirrel-cage winding on the rotor in order to increase the output of the machine and also perhaps to eliminate the stator current surging. If so, the rotor will be slotted, and coupled with the decrease in the air-gap, an additional source of loss that has not been considered in the paper, is likely to show an appreciable effect as in induction motors, namely the iron loss occurring in the stator and rotor teeth at high frequencies due to tooth ripples in the zigzag leakage flux, which will, however, remain entirely absent if a smooth solid rotor is used.

The factors on which this loss depends are discussed by Barton and Ahmad.* If the necessary precautions are not taken in design, it may become a predominant source in large and high-speed asynchronous generators, being large in stator teeth because of large ampere-turn concentration therein under normal operating conditions, and in rotor teeth because of their being unlaminated. In certain cases it may come out to be even more, but generally it is likely to be comparable with the eddy-current losses in the rotor surface and the pulsation losses in the rotor iron (Section 4.1) taken together, which are due to the tooth ripples in the main flux.

I am not quite sure about the type of rotor used in the experiments described in Section 4.3, but I presume that the losses increased rapidly as the ratio of the stator and rotor slot-pitches departed more and more from unity, and if so, the increase may be taken as due partly to the zigzag leakage-flux component and partly to the main flux component of the tooth-frequency loss. Theoretically, the zigzag leakage-flux component of the loss may be completely eliminated by making the stator and rotor slot-pitches equal, but in practice it may be considerably reduced by making their ratio as near unity as possible, the actual number of either stator or rotor slots having practically no effect on it.

[The author's reply to the above discussion will be found on page 346.]

NORTH-EASTERN CENTRE, AT NEWCASTLE UPON TYNE, 24TH FEBRUARY, 1958

Mr. W. D. Horsley: The author's interesting study is of value in showing that large high-speed asynchronous alternators of large output are practicable, even if it is not immediately possible to form any definite conclusions in regard to their application.

The additional cost of a combination of asynchronous and synchronous units would only be justified for systems normally operating at leading power factor or if larger and more economical units were thereby made practicable.

For systems where shunt reactors are a necessity, the asynchronous alternator appears to have possibilities, although it is not possible to generalize, and such projects would have to be considered individually. The cost of conventional reactors is less than that of high-speed synchronous machines of equivalent capacity. On the other hand, the cost of a high-speed asynchronous compared with a synchronous machine would be reduced owing to the simple rotor construction. In addition, it should be possible to reduce the cost by taking advantage of the simplified construction of the rotor to increase the rating. The end-leakage flux would be increased, but there are various means which could be adopted to minimize its effect. The tests on the 60 MW alternator referred to in Fig. 11 show that the temperature rises of the ends even under leading power-factor conditions are moderate, and some margin is available for increasing the rating.

In this connection, the author suggests that, with an asynchronous rotor construction, the abrupt change in section at the ends of the body could be eliminated. The mechanical design would be improved, but the length of the leakage path for the end-leakage fluxes would be shortened and the losses correspondingly increased. Some compromise would have to be made.

The losses in an asynchronous rotor could undoubtedly be made small. The slip characteristics given in Fig. 5 indicate low values of loss in a conventional rotor operating asynchronously. The tests on the 60 MW set referred to gave values of slip at full load of about $0\cdot3\%$ and $0\cdot7\%$ respectively with and without the field winding connected.

In comparing leakage fluxes at different loads, the author has neglected the effect of saturation and variations in the reluctance of the leakage paths with the rotor angle. The curves in Fig. 11 seem to indicate that the effect is not great, and the results with magnetic caps would be of interest in this connection.

The paper adds to our knowledge of end heating and shows that the technical problems likely to be encountered in the design of high-speed asynchronous generators are not formidable, and that for a few applications an economic study would be worth while.

Mr. R. A. Hore: The author has discussed core end-heating principally as it affects synchronous machines. I would have thought that the virtual absence of rotor end-winding and the absence of end bell would have considerably alleviated the problem in the case of asynchronous machines.

Self-excitation is rather more of a problem than indicated; if the machine overspeeds the frequency rises, the open-circuit excitation characteristic rises and the capacitance characteristic falls, so that smaller values of capacitance suffice to produce the

* Barton, T. H., and Ahmad, V.: 'The Measurement and Prediction of Induction Motor Stray Loss at Large Slips', *Proceedings I.E.E.*, Monograph No. 219 U, January, 1957 (104 C, p. 229).
Barton, T. H., and Ahmad, V.: 'The Measurement of Induction Motor Stray Loss and its Effect on Performance', *ibid.*, Monograph No. 255 U, September, 1957 (105 C, p. 69).

phenomenon (see Fig. 2). The actual voltage produced is, however, usually limited to something less than twice normal by saturation of the machine and of system transformers.

The author's proposals for cross-compound sets are only attractive if the overall power factor of generation is about the same as that of the asynchronous machine, since (Fig. 16) the shortest distance between two points is a straight line. However, in long-distance transmission it is often difficult to reach surge impedance loading, and thus the generation power factor may well be predominantly leading. D.C. transmission, where no reactive power whatsoever is transmitted, may also be a case where spare reactive capacity is cheaply available at the generation point for exciting an asynchronous machine. On a smaller scale, one rather special case of interest is the Hebrides scheme, where the charging current of submarine cables much exceeds the generation capacity; some synchronous plant is, however, essential for fine voltage control.

Mr. T. H. Milne: The current inrush on switching in the asynchronous generator will be of short duration, and for this reason may not be objectionable, but will there be shock to shaft couplings, especially if the speed is not exactly synchronous on switching in? If so it would seem desirable, as a precaution, to employ a slip-measuring relay, possibly interlocked with the breaker closing circuit.

It is regrettable that no reference to switching has been made in connection with the cross-compound examples in Section 5. Presumably the author has in mind the use of separate circuit-breakers for the two generators. In circumstances where a single circuit-breaker is appropriate the excitation for the asynchronous generator will be drawn from the synchronous unit when the latter's excitation is built up preparatory to synchronizing with the system. In this case there need be no shock or inrush on switching in, but it would be necessary for the synchronous machine to be capable of delivering the no-load lagging output required.

The two examples given in Section 5 illustrate how the choice of power factor for the asynchronous unit will depend largely on the system conditions. In the first example the optimum 0·9 has been chosen presumably to keep the synchronous machine's reactive loading to reasonable proportions, whilst in the second example the assumed system power factor of 0·9 leading provides scope for the asynchronous machine to adopt an easier design. Conditions in this country favour the first rather than the second example, and it is a pity that the estimation of cost and efficiency should be based on the latter.

With regard to core-end heating, it would seem from a consideration of Fig. 10 that the worst flux condition is experienced if the machine slips poles, because then the stator and rotor vectors become additive and the resultant passes through a maximum. Although the duration of such conditions is short, they affect mainly only two or three laminations of core plate and hence a considerable temperature rise may result, especially after several pole-slips.

Mr. F. H. Birch: It is stated in Section 2.6 that an asynchronous generator does not impose a duty on circuit-breakers. Whilst this statement is probably intended to relate to a 3-phase fault condition, it must be recognized that 3-phase faults comprise less than 10% of the faults experienced on the Grid system in this country. The remainder are unbalanced faults involving only one or two of the phases and generally earth in addition. Large generators in this country are now switched at 275 or 132kV and the h.v. neutrals of their step-up transformers are directly earthed. This form of earthing, coupled with the fact that with unbalanced faults the asynchronous generator will have a field maintained by the sound phase or phases, suggests that the circuit-breaker duty would be far from negligible.

In Section 2.7 the author recommends the fitting of loss-of-excitation alarms wherever possible. The general omission of these alarms in the past has not been embarrassing, owing, probably, to the fact that loss of field has generally been accompanied by noisy operation of the voltage regulators. If the machine is on full load when its excitation is lost, it may trip out on over-speed, but if this does not happen the increase in stator current may be just sufficient to cause operation of the back-up over-current relay.

It would appear that the loss of efficiency arising from the use of an asynchronous generator would outweigh the advantages described in Section 5.2. Assuming a reduction in alternator efficiency of 1%, an overall thermal efficiency of 35% and coal at 11 500 B.Th.U./lb costing 70s. per ton delivered, it is calculated that with an average load factor of 70% the cost of additional coal consumed on account of the reduced alternator efficiency would amount to £420 000 over the 25 years' life of the machine. If the capital cost of the alternator is taken as £2 per kilowatt, this is slightly less than the extra coal cost, which is therefore prohibitively high.

Mr. H. D. Briggs: The small air-gap required for the asynchronous generator (Fig. 4) would certainly seem to present a problem in the larger sizes, owing to the restriction of ventilation in the air-gap. Another point is that the reduced air-gap may result in an undesirable tooth ripple, causing harmonics in the stator windings.

Referring to the cross-compound unit suggested in Section 5.2.1, it occurs to me that there may arise a turbine problem worth considering. In the arrangement described, one has an asynchronous generator in the high-speed line and the shaft may be said to be 'loosely' tied to the system frequency, i.e. it would not be running at synchronous speed or at any fixed ratio to it. On the other hand, in the low-speed line the shaft driving the synchronous generator would normally be tied 'rigidly' to the system frequency. This would seem to present a difficult governing problem in a cross-compound turbine set, and I would be interested to know if the author has considered this point.

Mr. G. H. Hickling: It has been suggested during the discussion that the use of a small air-gap in an asynchronous generator, for reasons of efficiency, may incur risk of contact between rotor and stator as the result of any unbalance vibrations occurring in the rotor. In contrast with this view, however, I believe that such generators, having solid squirrel-cage rotors with no insulation, are likely to be particularly free from unbalance troubles, and that no such danger should therefore arise. Any balancing difficulties which have up to now been experienced with conventional alternator rotors have always been associated with the winding and the slot insulation, the end packing arrangements, the end-bells, and (where thermal unbalance effects have been experienced) the methods of removing heat from the insulated winding conductors. Since the projected asynchronous alternators would have none of these components, all the main causes of unbalance, after initial balancing has been done, should be eliminated.

Dr. B. C. Robinson: In Fig. 2 the magnetization curve is given for an induction generator. At first this appears impossible as there is no excitation on the machine windings to produce any e.m.f. If, however, the machine has sufficient residual magnetism it will generate a small voltage, which will build up until the appropriate operating point A_1 or A_2 is reached. Could the author indicate whether he considers this to be a practical method of running up an asynchronous generator, or is the residual magnetism insufficient? It would appear that the best method of bringing an asynchronous generator into service would be to run it up simultaneously with a synchronous machine and then to

synchronize the combined unit on to the busbars. Perhaps the author could indicate the voltage and frequency characteristics of an asynchronous machine when isolated from a synchronous network, or is this an impossible mode of operation?

It would appear that this form of operation could be better described as capacitance-excitation rather than self-excitation.

[The author's reply to the above discussion will be found overleaf.]

NORTH-WESTERN SUPPLY GROUP, AT MANCHESTER, 8TH APRIL, 1958

Mr. C. Ayers: In Sections 5.2.2 and 5.2.7, the author puts forward an appreciation of the design and financial facets of the application of asynchronous machines coupled with synchronous generators to form one compound generating unit.

If we consider the problem from a slightly different standpoint we find that the cross-compound unit is at a serious disadvantage. This is evident from Figs. B and C.

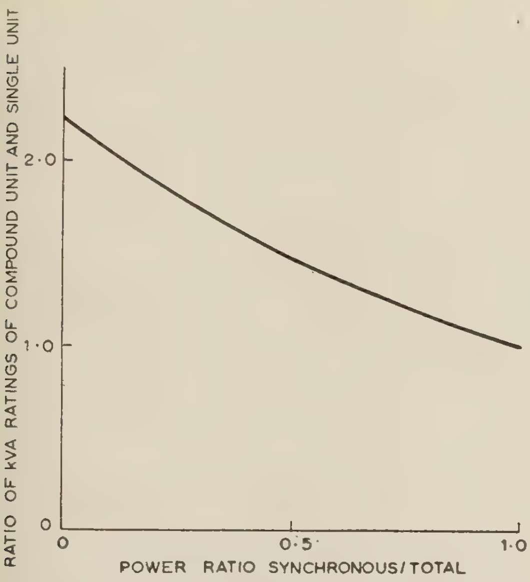

Fig. B

Basis: constant power sent out; system power factor, 0·8 lagging; asynchronous-generator power factor, 0·8 leading.

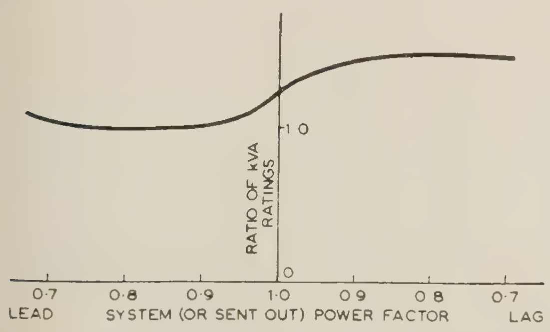

Fig. C

Basis: constant power sent out; power ratio synchronous/total, 0·5; asynchronous-generator power factor, 0·8 leading.

Fig. B attempts to show the ratio between the kVA ratings of a compound (asynchronous-synchronous) unit and a conventional machine having the same power output, for various fractions of power from the synchronous portion of the set.

Fig. C shows the effect of variation in the system or sent-out power factor for a power ratio (synchronous/total) of 0·5, on the ratio of the kVA ratings.

The constant factors used in this analysis are an asynchronous-generator power factor of 0·8 leading and in the case of Fig. B a system power factor of 0·8 lagging.

It is evident from the diagrams that for all power ratios the compound set is at a disadvantage when compared with a straight machine. It is also at a disadvantage at all system power factors except where the power factors of the asynchronous machine, synchronous machine and the system are all equal and in the leading region.

For economic design, therefore, it appears that the compound set must be allied to the system power factor in a fairly rigid manner, and even if this is done the installed capacity of the set is higher than that of a conventional machine. If this is true, there does not appear to be a case for the installation of a compound set, even when systems demand a leading power factor.

Mr. W. W. Holburn: It is surprising to find that the author does not fear the possibility of overheating or damage occurring to the rotor due to asynchronous operation. His conclusion, which may be valid in the case of solid rotors machined from single forgings which are not provided with additional damper windings, should not be regarded as generally applicable. Reports have been received describing damage of a similar nature to that which can result from unbalanced loading, when the circumstances have indicated that the cause has been loss of excitation and/or loss of synchronism. The evidence was most marked in the case of an old 4-pole generator which had a composite rotor made from three separate forgings and was fitted with a copper damper winding. This machine pulled out of step and ran with heavily fluctuating load for about 15 minutes. The rotor was examined immediately after the disturbance, and it was necessary to replace some wedges which were badly damaged. There were also signs of overheating at the joints between adjacent rotor body sections, clearly indicating that the damper winding had been overloaded.

It is the practice of several manufacturers to provide a damper winding forming a squirrel cage near the surface of the rotor. It is claimed that the virtue of the winding is to provide an unbroken path for the circulating currents which flow during conditions of unbalanced load, and which otherwise might cause local heating at points of high resistance, e.g. between slot grooves and wedges at each end of the rotor body. If this function is recognized there is a strong case for increasing the efficiency of the damper winding as specific ratings are increased, and the dangers of overloading the winding, either by exceeding accepted negative sequence current limits or by asynchronous operation, should not be neglected.

Dr. K. C. Mukherji: Assuming that the rotor of the 2500 kW turbo-generator did not carry any damper winding, a curve corresponding to curve B in Fig. 3, which applies to a perfectly solid rotor, would lie between the two curves of Fig. 5. It would appear, therefore, that the author's estimate of the slip-frequency losses in the solid rotor of an asynchronous generator, based on tests on a normal turbo-type rotor, may have been rather optimistic.

The resemblance between the calculated curves and test results in Fig. 11 is striking indeed, considering the author's simplification of the problem, for it is difficult to follow how the

vector OA in Fig. 10, said to represent the rotor-winding leakage flux, can be fixed in magnitude and direction by the air-gap ampere-turns alone, and not by the total rotor ampere-turns acting under actual load conditions.

I should like to know more about the conditions of the measurements on which Fig. 15 is based, for although this diagram serves to give us an overall picture, it is useful to know what in fact the flux variations were due to. As the stator slot-pitch was increased, presumably the stator slot-width was increased as well. Did the measurements include any variations due to flux tufting?

In this connection it is of interest to note that Rudenberg* suggests that the air-gap should not be less than 40% of the stator slot-pitch if excessive stray harmonic losses are to be avoided. We could check Rudenberg's rule if we knew the stator bore diameter.

If we have a steadily tapered, stiff shaft, would not the core end-iron heating correspond to asynchronous operation with a magnetic end-bell rather than with a non-magnetic end-bell as shown in Fig. 11?

Mr. J. Tozer: In Section 5.2.7 the author claims that savings may accrue from the reduced cost of switchgear, bearing in mind the characteristics of the asynchronous generator which reduce the breaking capacity. It should be pointed out that the generator circuit-breaker would no doubt be part of a group of switchgear on common busbars which would require a certain breaking capacity to meet the demand of the system, so that the saving claimed could not, in fact, be effected.

THE AUTHOR'S REPLY TO THE ABOVE DISCUSSIONS

Mr. P. Richardson (*in reply*): The operation of generators on loss of excitation has been clarified, and it will be observed from Fig. A that the percentage slip on loss of excitation at full load becomes less as the rating of the generator is increased. The rotor body end conditions, which are akin to the end ring of a squirrel cage, remain substantially constant, while the length of the rotor increases with output much faster than the diameter; the true explanation is probably associated with this relationship. I consider that most of the damping on a turbo-type rotor is associated with the solid rotor construction; the incorporation of heavy damping windings would entail removal of active copper from the rotor slot, and the consequent increase in frame size would not justify the additional damping effect. It would appear evident that problems of heating are associated with the stator core end and not with the rotor, and from the description of the type of damage experienced, the specific case of rotor heating would seem to have been associated with a negative-sequence fault. The calculated pull-out torque for the 60 MW generator mentioned in the paper is just over twice the rated full-load torque, and I agree that where the system falls below normal the pull-out torque of an asynchronous generator falls rapidly.

While I agree that the temperature-rise figures in Fig. 11 are moderate owing to the relatively low ampere-turn rating, I would not necessarily expect to provide end-field dampers with more highly rated machines, as the gas pressure may be increased, thus leading to improved cooling. Thermocouples were positioned in the end plate and in the core end sections, and the maximum temperature rises shown in Fig. 11 were recorded on the end sections of the stator core. This test was interrupted midway, and the opportunity was taken to replace the non-magnetic rotor end bells with a set made of magnetic material, this being done without in any way disturbing the thermocouples in the stator core end. The curve shown in Fig. 11 was repeated in shape but at a temperature rise just over twice that shown. This confirms that the general theory of core end heating applies equally well to the two different types of rotor end bells. While one would at first sight expect the core end heating to vary as the square of the specific loading, I would point out that the stator heating depends upon the sum of two vectors, one of which is substantially constant depending upon the air-gap flux and the other varying directly with the stator loading. With a high short-circuit ratio, for example, the core end heating will vary less with the specific loading than with a machine having a low short-circuit ratio. I appreciate that the final temperature rise of the stator core end must be a combination of heating resulting from the end leakage field and that which is associated with the normal machine iron and copper losses, and therefore represents only a first approximation but one which gives a reasonable indication of the effects of changes in design. I would expect the use of a non-magnetic end plate to increase the length of the flux path in the end windings and on this account to reduce the level of heating. On the other hand, the maximum flux densities are associated with the stator core end in the vicinity of the conductors and no material difference is likely. Stator-core end heating is not noticeable in multi-polar generators as the ampere-turn rating per pole becomes appreciably less than in 2-pole generators. The stator-core end-flux conditions during pole slipping will be severe owing to the large stator current when the generator is 180° out of phase.

The curves shown in Fig. 15 are expressed in terms of percentage loss and thus enable a comparison to be made between different arrangements of stator slots. The dummy rotor consisted of a heavy steel plate bent to conform with the stator bore. The information from these tests and from the temperature rise of rotors in commission has confirmed that the factors concerned are of importance. Losses due to flux tufting can be severe, and while rotor slots can be closed with magnetic wedges, care must be exercised in the design of the stator slot.

The assessment of cost and efficiency was included to illustrate the associated factors, and in selecting the variables for the comparison no account was taken, for example, of short-circuit ratio. Under normal circumstances a synchronous unit required to operate at leading power factor would be associated with a short-circuit ratio of 0·9 or unity for stability reasons, but with the asynchronous-synchronous combination it should be possible to reduce the short-circuit ratio of the synchronous unit to a nominal figure of, say, 0·55 and thus offset at least part of the apparent disadvantage of the combination. On the other hand, present-day developments associated with automatic voltage regulators may well justify the selection of a lower short-circuit ratio for the equivalent synchronous unit.

The stability problems associated with asynchronous generators are perhaps worthy of further study. A number of hydro-electric installations of asynchronous plant, although, of course, of relatively small output, appear to operate satisfactorily. With regard to transient stability, the tie between synchronous and asynchronous plant is fairly solid under normal conditions. The latter is able to contribute effectively to the inertia of the system, and while the damping power which can be provided is normally neglected in a system study, it is recognized as one of the factors which assist in improving stability.

* RÜDENBERG, R.: 'Zusätchliche Verluste in Synchronmaschinen und ihre Messung' *Elektrotechnische Zeitschrift*, 1924, 45, p. 37.

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RESULTS OF FULL-SCALE STABILITY TESTS ON THE BRITISH 132 kV GRID SYSTEM

By F. BUSEMANN, Dr.Ing., Associate Member, and W. CASSON, Member.

(*The paper was first received* 27*th March, in revised form* 4*th November*, 1957, *and in final form* 9*th January*, 1958. *It was published in February*, 1958, *and was read before the* NORTH MIDLAND CENTRE 4*th February*, THE INSTITUTION 6*th March, and the* NORTH-EASTERN CENTRE 10*th March*, 1958.)

SUMMARY

The paper describes the results obtained from transient stability tests carried out on a section of the British 132 kV Grid system during the summer of 1956. A 45 MW (56 MVA) generator was connected to the main system through a 150-mile line and faults of various types were applied with the generator on full load. The main purpose of the tests was to check how closely network-analyser results agreed with the actual performance. It was found that the agreement was, in general, very good, and some important information was obtained about the characteristics of cylindrical-rotor machines which should be of assistance in future analyser studies.

It was also found that flux decay, even with voltage regulators, is a factor which alone could result in discrepancies between network-analyser studies and actual performance, but that this is largely compensated, when it is neglected, by the improving effects of damping, additional losses during faults and governor action.

Stability was maintained with full load on the machine when the reactance of 150 miles of line was suddenly inserted between it and the system, provided that the excitation was sufficient for steady-state operation with the line. Reactive power consumed by the machine at the steady-state stability limit was about 15% higher than calculated values, owing to saturation effects. During asynchronous operation with open field up to 30 MW the slip remained very low.

LIST OF PRINCIPAL SYMBOLS

V_s = Voltage behind synchronous reactance.
V_t = Voltage behind transient reactance.
ΔV_t = Decay of voltage behind transient reactance during fault.
V_p = Voltage behind Potier reactance.
I = Current.
I_n = Rated current.
LG-fault = Fault between line and earth.
LL-fault = Fault between two lines.
LLL-fault = Fault between three lines.
P_e = Electric power output of the alternator.
P_m = Mechanical power input of the alternator.
R_t = Total transformer resistance per phase.
s = Slip, i.e. difference between rotor speed and synchronous speed, as a percentage of the synchronous speed.
s_e = Equivalent slip for rated damping torque (apparent-power rating).
t = Time from beginning of fault or disturbance.
t_{max} = Maximum fault clearing time.
T_d = Damping constant, i.e. percentage of damping torque per percentage of slip.
t_d = Time-constant of attenuation of electro-mechanical oscillations.
V_1 and V_2 = Voltages at generator and system end of line, respectively.
V_n = Rated voltage.
X_q = Quadrature-axis synchronous reactance.
ψ = Phase displacement of stator current with respect to voltage corresponding to rotor axis.
X_t = Transformer reactance.
Z_l = Line impedance.
δ_1 = Angle between V_s and V_1.
δ_2 = Angle between V_s and V_2.
δ_3 = Angle between V_p and V_1.
ϕ_1 = Angle between V_1 and I.

(1) INTRODUCTION

The stability of an electrical power system may be defined as the ability of the alternators in that system to work in step with one another under slow changes (steady state) or rapid changes (transient state) in the operating conditions.

It is the aim of system designers, and particularly those concerned with the design of large national Grid systems or systems employing long-distance power lines, to ensure that their systems will be stable under normal conditions of operation and under fault conditions and that this will be procured at the lowest cost.

If there are limitations in the use of a system on account of stability which do not allow full flexibility in operation with a minimum of spare capacity and can result in loss of supply when emergencies occur, it would seem to be worth while investigating what improvements can be made. This may not be achieved by suitably modifying one particular piece of equipment and may involve the provision of better voltage regulators and damper windings for the alternators and better governors for the prime movers. It may also involve the reduction of reactance in the system by employing auto-transformers, overhead lines with bundle conductors and series capacitors.

The requirements of the load for reactive power and voltage regulation have usually to be considered in conjunction with measures adopted for improving stability and may require the use of synchronous condensers which can be designed both to generate and consume reactive power.

Alternators have to be designed to generate reactive power as well as active power, but to improve stability it may be necessary to provide a larger air-gap, which would require more magnetizing ampere-turns on the rotor. However, with the winding already squeezed into the available space, this may only be achieved at the expense of increased rotor losses and cooling.

The only satisfactory way of determining the stability of a system and those measures which can be adopted to make an improvement in this respect is to carry out studies on a network analyser. The types of network analyser generally used are single-phase analogues which are set according to design data obtained in respect of the alternators and other plant. Determination of the stability limit by these analysers is a laborious step-by-step process. This can be avoided to some extent by the use of *micro-réseaux* (miniature networks), but they cannot be set perfectly to simulate the performance of large units.

In view of the increased use of network analysers for solving

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stability problems on complex transmission systems, Panel V/Da of the E.R.A., dealing with system constants, felt that, although full-scale system tests have been carried out previously[1–5] which have produced valuable information on various aspects of the behaviour of a system under abnormal conditions, none have been staged primarily for determining how accurately network-analyser studies represent actual system conditions. It was decided, therefore, to see if tests specifically for this purpose could be carried out on the British Grid system. It was first necessary to seek the co-operation of the C.E.A. in providing a section of the Grid system in which an isolated generator or generators could be connected to the main Grid system through a high-impedance link so that instability could be produced under steady-state and transient fault conditions without causing any great disturbance. It was found that the desired conditions could be obtained at Cliff Quay (Ipswich) generating station, where there is an installation of 6–45 MW (56 MVA) turbo-generators connected to a 132 kV busbar, to which are also connected six lines and two local supply transformers. It was possible to arrange for one of the generators and one end of a line circuit 150 miles in length to be connected to one busbar (called the 'test bar') and for the other end of the line to be connected to another busbar (called the 'system bar'), together with the remaining generators, lines and local transformers.

The Chief Engineer of the C.E.A. gave permission for full-scale stability tests, including transitory faults, to be carried out from 4th to 7th August, 1956. Prior to the tests, studies were carried out on two types of network analyser in Britain and on the *micro-réseau* analyser in Paris, assuming circuit and load conditions which were to be expected when the system tests were carried out.

(2) DETERMINATION OF STABILITY AND SYSTEM PERFORMANCE

(2.1) Vector Diagrams of a Cylindrical-Rotor Synchronous Machine connected to the System through a Long Line

Fig. 1(*a*) shows a simple diagram of a generator supplying a load through an impedance which represents the test conditions applicable at Cliff Quay generating station, where the coupling between the two busbars was a 150-mile line.

Fig. 1(*b*) shows the conditions when the stability limit is nearly reached with a lagging load on the generator and illustrates the limitations of transmitting power through a long line. OV_1 is the h.v. terminal voltage at the machine, i.e. the test busbar, and OV_2 is the voltage at the system busbar. The voltage drop in the line impedance is V_1V_2, and the current I lags the voltage V_1 by the angle ϕ_1. $I(R_1 + R_t)$ is the voltage drop in the armature and transformer resistance in phase with the current I, and $I(X_d + X_t)$ is the voltage drop in the synchronous and transformer reactance in quadrature with I. OV_s is the voltage behind the synchronous reactance and δ_2 is the power angle between the rotor and voltage V_2, which is approaching 90°. In quadrature with the voltage OV_s is the rotor current I_{rot}. The vector difference between the rotor and the stator current provides m.m.f. to drive the magnetic flux through the machine corresponding to the voltage OV_p behind the Potier reactance and transformer reactance $(X_p + X_t)$.

Fig. 1(*c*) is the vector diagram for the same machine when the stability limit is nearly reached with a leading load on the generator and for the same voltage V_2 as in Fig. 1(*b*), and it illustrates the reduction in transmission of power due to weak excitation of the generator. It is observed that the power angle δ_2 is approaching 90° and the voltages behind the Potier and synchronous reactances are much smaller than in the previous case. The voltage V_1 is very much reduced and the power generated is less than one-half that in the previous example.

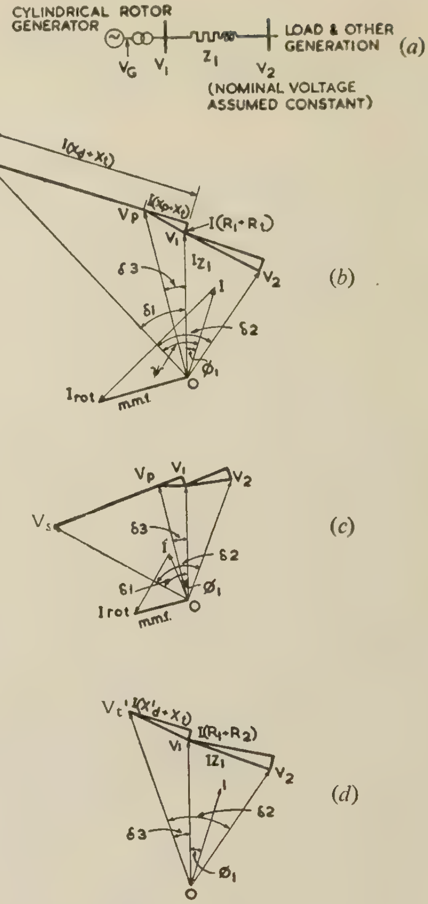


Fig. 1.—System and vector diagrams.

(*a*) System considered.
(*b*) Vector diagram of conditions reached in the system shown in (*a*) at about the stability limit, with lagging current on the generator, i.e. δ_2 approaching 90°.
(*c*) Vector diagram of conditions reached in the system shown in (*a*) at about the stability limit with leading current on the generator, i.e. δ_2 approaching 90°.
(*d*) Vector diagram of the set-up on an a.c. network analyser for the system considered in (*a*) and load conditions of (*b*) for transient stability studies.

(2.2) Steady-State Stability Limit

A system is in stable equilibrium if the forces caused by a change in the position act in the direction against this change so as to restore the original position. In the case of a synchronous generator, stability is maintained if, with an increase in the power angle, an incremental torque occurs which would pull back the rotor so as to decrease the power angle.

The steady-state stability limit is reached when the torque increment of the synchronous generator working with constant excitation for a given power-angle increment is zero. Unstable conditions would occur if the torque increment were negative.

The steady-state stability limit of a cylindrical-rotor synchronous machine with negligible resistance in the stator circuit and without voltage regulator is reached when the power angle, i.e. δ_2 in Figs. 1(*b*) and (*c*), reaches 90°.

The term short-circuit ratio (s.c.r.) is the ratio of the excitation for rated open-circuit voltage at rated frequency to the excitation for 3-phase short-circuit current at rated frequency. The unsaturated direct-axis synchronous reactance (in per-unit values) is the reciprocal of its short-circuit ratio multiplied by the ratio of the straight-line value to the actual (saturated) open-circuit voltage for the same excitation.

For improved steady-state stability conditions it is desirable to design the machine for a relatively high short-circuit ratio (low synchronous reactance), e.g. by larger air-gap, to operate it with as small a leading reactive power as possible, and to use a tap on the generator transformer so as to have relatively high generator terminal voltage (high l.v. tap or low h.v. tap). This can be appreciated by inspection of the vector diagrams [Figs. 1(*b*) and 1(*c*)].

Saturation does not affect the stability limit of 90° for cylindrical-rotor machines (without voltage regulator), but it results in increased leading reactive powers at which this limit is reached.

Stability conditions can be effectively improved by the use of voltage regulators ('dynamic stability' if small changes of load and power are considered) as compared with the operation at constant excitation.

(2.3) Transient-Stability Limit

Transient stability is maintained if, after disturbances causing a large change of the power angle, a new equilibrium is established without loss of synchronism, i.e. without pole slipping. Fig. 2 shows a power/power-angle diagram in which A may be

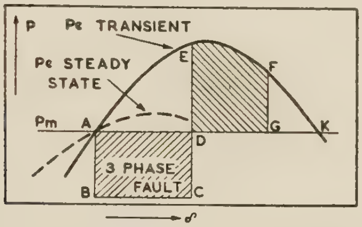


Fig. 2.—Power/power-angle diagram.

the working point before the disturbance where the mechanical input P_m equals the electrical output P_e. During a close-up h.v. 3-phase fault the electrical output drops to a value equal to the transformer and machine losses, so that the rotor is accelerated and the power angle increased and the working point changes during the fault along the line BC. After the fault is cleared, the electrical output is raised from C to E, but, owing to the energy sent into the rotor during the fault (which is proportional to the area ABCD), the rotor overspeeds, i.e. it slips, and the power angle continues to increase until F is reached, when the slip has been reduced to zero. Afterwards the electrical output still exceeds the mechanical input and the machine swings back around a point in the diagram where P_m equals P_e. For a machine working on an infinite busbar system, transient stability can be determined by the 'equal area' theorem, ABCD = DEFG, assuming constant flux linkages; transient stability would not be maintained if the area passed during the fault, ABCD, had been greater than DEK. If the power angle still increased beyond the point K, the rotor would be accelerated again and pole slipping would occur.

The electrical output P_e, available during transient conditions, differs from that in the steady state (Fig. 2), because, during transients, the magnetic flux cannot change as rapidly as the power angle. In transient studies it is therefore usual to assume that the voltage behind the transient reactance X'_d remains constant, and the power angle used in transient studies [see Fig. 1(*d*)] is referred to this voltage, which is thus smaller than the actual power angle with respect to the rotor axis, i.e. the voltage behind the synchronous reactance.

Transient-stability conditions in respect of the first swing are improved with a higher inertia constant in the alternators (i.e. smaller variation of power angle for given conditions), shorter fault clearing times (i.e. reduction of the first area) and measures ensuring the increase of the area available after the fault such as lower reactances (i.e. lower transient reactance in the alternators and lower external reactance), the provision of more damping in the alternators (i.e. resultant asynchronous torque provides a higher electrical output when the angle δ_2 increases than when it decreases), improved voltage regulator action (i.e. maintaining high electrical output) and improved governor action (i.e. reduced mechanical input will improve performance).

(2.4) Network-Analyser Studies

Network-analyser studies of transient stability usually involve more than two points of generation which make it difficult to apply the equal-area theorem. Transient-stability conditions are therefore considered by means of 'swing curves', i.e. power angle/time curves obtained from electrical output readings for every generator taken step-by-step for short time intervals after each of which the phase angle of the e.m.f.'s of the generators are adjusted to a new position according to the difference between mechanical and electrical power, for known inertia constant and slip. Stability is maintained if the power angle reaches a maximum without pole slipping of any generator. Machine data used in transient stability studies are as follows:

(*a*) *Transient Reactance.*

This is calculated from a short-circuit test oscillogram.

(*b*) *Inertia Constant.*

The factors usually neglected include

(i) Flux decay during the fault. This occurs owing to the high inductive component of the fault current.
(ii) Voltage-regulator action.
(iii) Governor action.
(iv) Damping.
(v) Losses [other than $I^2(R_1 + R_t)$ losses in stator and transformer].

(2.5) Micro-Réseau Studies

For the study of transient-stability conditions, the *micro-réseau*, developed in France,[6] has the advantage that results are obtained immediately by inspection (i.e. whether the system is stable or unstable) or on oscillograms without the laborious step-by-step procedure which is necessary with the conventional types of network analyser. Also some of the usually neglected factors can be introduced more readily, such as the action of voltage regulators or governors and to some extent the effect of damping.

However, a disadvantage of the *micro-réseau* is that it is not possible to represent accurately the losses in the machine simulated. This is because the miniature machines are only of the order of 1 kW in size, and since the relative losses of machines increase with about the fourth power of the decrease in size, it is seen that the losses of a 1 kW machine would be relatively out of proportion to those of a larger machine. This can be compensated to some extent by auxiliary machines or devices connected in series with the stator and rotor circuit, producing negative-resistive voltage drop proportional to the stator and rotor current.

(3) SCOPE OF THE TESTS

The main purpose of the tests was to obtain information on the transient performance of synchronous machines on system fault conditions of varying complexity and to assess the magnitude of the errors introduced individually by the simplifying assumptions made in routine network-analyser studies. Some of these assumptions worsen the conditions, e.g. flux decay, and others improve them, e.g. action of voltage regulators, governors, damping and additional losses during the fault. It was also

Table 1
MEASUREMENTS

Pos.	Item	Symbol	Method d	D	M	P	C
a	Steam pressure	p_{st}		D			
b	Exciter field voltage	V_{ee}	d	D			
	Exciter field current	I_{ee}	d	D			
c	Generator, rotor voltage ..	V_{rot}	d	D			
	Generator rotor current ..	I_{rot}	d	D			
e	Generator power angle	δ	d	D		P	C
f	Generator, 11·8 kV voltage ..	V_{11kV}	d	D	M		
	Generator, stator current ..	I_{11kV}	d	D	M		
	Generator, power	P_{11kV}	d	D			
	Generator, reactive power ..	Q_{11kV}	d				
g	Fault current	I_f		D			
	Fault current, zero sequence ..	$(3I_0)_f$		D			
	Voltage across fault-clearing breaker	—					C
	Trip-coil current	I_{coil}		D			
	Voltage on 132 kV test bar ..	V_{test}	d	D	M		
h	Current on line test bar to C ..	I_{test}		D	M		
	Current, zero sequence	$(3I_0)_{test}$		D			
i	Voltage difference test-system busbars	ΔV		D	M		
j	Voltage on 132 kV system bar ..	V_{syst}	d	D	M		
	Current on line C to system bar	I_{syst}		D			
	Power on this line	P_{syst}		D			
k	Voltage, unit transformer 3 kV	V_{3kV}	d		M		
	Current, unit transformer 3 kV	I_{3kV}	d		M		

d = Direct-reading instrument.
D = Duddell oscillograph.
M = Masson oscillograph.
P = Pen recorder.
C = Cathode-ray oscillograph.

desired to assess the error introduced by uncertainties or tolerances in the data used.

The secondary purpose of the tests was to obtain information about the steady-state performance of synchronous machines with weakened fields, the sudden insertion of impedance in the system and the performance of alternators when operated asynchronously.

(4) TEST ARRANGEMENTS AND PROGRAMME

The test arrangement is shown in Fig. 3. One of the 45 MW generators (called the 'test generator') was connected to a section of 132 kV busbar AI, which could be connected to the 'system busbar' AII, either directly by closing the switch S1, or through the double-circuit line to substation C and back by closing the switches S4 and S5, or to a section of busbar AIII and from there to substation DI (13 mile line) by closing the switches S2 and S3. Three generators were connected to the 'system busbar', together with three lines connected to the main Grid system.

The fault-throwing connections were made on the line to DI substation, which was open at the substation end. One test was carried out with the fault at the DI substation end of the line and the remainder at the Cliff Quay end. In each case the fault was applied by closing switch S3 and removed by tripping switch S2.

Table 1 shows the quantities measured by direct-reading or recording instruments, according to the positions (*a*) to (*k*) indicated in Fig. 3.

In addition to the measurements of direct interest for the investigation of stability, the restriking voltage at the circuit-

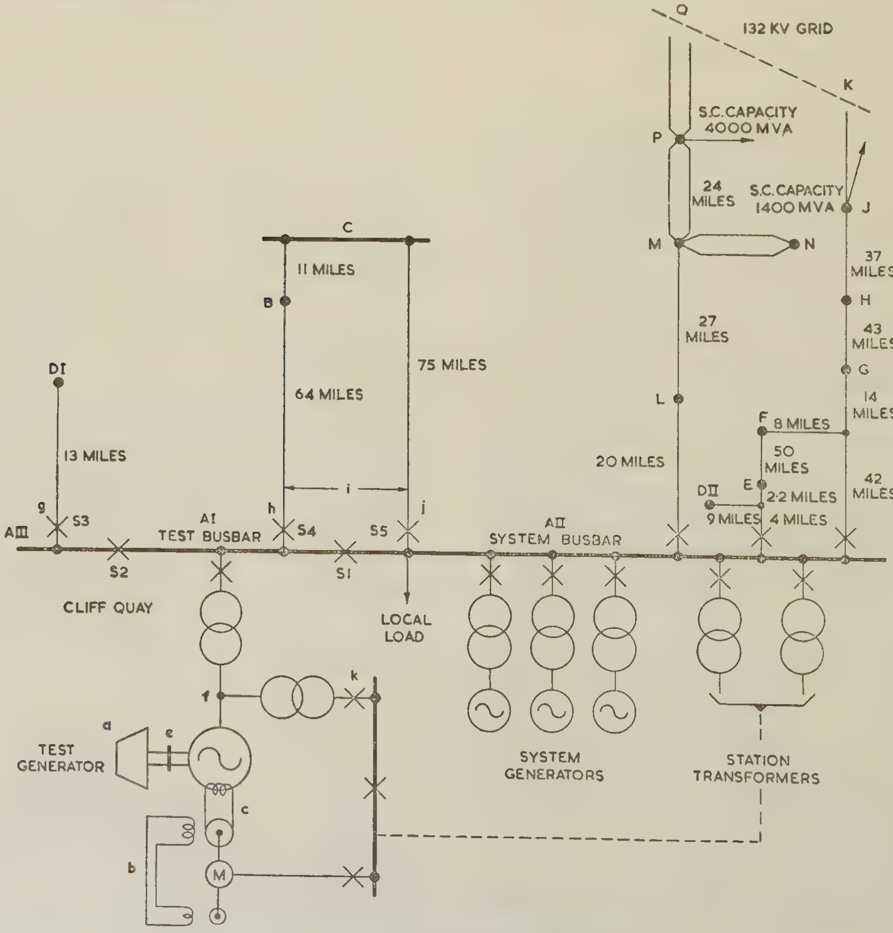


Fig. 3.—Test arrangement of the system.

breaker clearing the fault was recorded by a cathode-ray oscillograph, but the results are not given in the paper. The tests also afforded a manufacturer an opportunity of investigating the performance of special relays designed to prevent the tripping of circuit-breakers near the phase opposition of voltages under conditions of instability.

Instruments for direct reading of power angle were provided and tried out by the C.E.A. Research Laboratories, London Generating Division,[7] the North of Scotland Hydro-Electric Board and the Imperial College of Science and Technology. A further offer of direct-reading-type instruments from a manufacturer had to be declined because of lack of time for preparing attachments to the shaft end of the turbo-alternator.

Instruments for direct reading, numerous facilities for measurements on the secondary windings of existing instrument transformers, Masson recorders, and communication facilities were provided by the Eastern Division of the C.E.A., in whose hands were also the control of the tests and co-ordination of the programme.

In all tests except one, the exciter for the test generator (all the generators at Cliff Quay have separately driven exciters) and certain other auxiliaries were driven from the unit transformer. Turbine and boiler main auxiliaries were fed from the station supply.

The mechanical input of the 'test generator' was covered by recording the steam pressure of the turbine. The governor movement itself was not recorded by an oscillograph but was televised to the station control room. Some film records were made, but they are not included in the paper.

Besides measurement of power angle between the rotor test generator and various voltages, such as the generator 11·8kV terminal voltage, or the voltage at the 132kV test or system busbar, recordings were made of the angle between the rotor of another generator and the system busbar voltage, and of the angle between the 'system busbar' at Cliff Quay and the voltage at a distant Grid point, Leatherhead, some 30 miles south of London. The latter recordings were made by the C.E.A. Research Laboratories.

Table 2

PROGRAMME OF TRANSIENT TESTS

Test No.	Load	Voltage regulator	Fault Type	Fault Distance	Duration	Objective
	MW			miles	sec	
1·1	45	Yes	LG	13	0·62	Transient performance of generator and system on faults
1·2	45	Yes	LL	0	1·28	
1·3	45	Yes	LL	0	0·96	
1·4	45	No	LL	0	0·98	
1·5	30	Yes	LLL	0	0·68	
1·6	45	Yes	LLL	0	0·32	
2	0	No	LLL	0	0·94	Additional losses during fault
3·1	15	No				Transient performance with sudden insertion of 150 miles of 132kV line
3·2	45	No				
3·3	45	Yes				

The programme for the transient tests is shown in Table 2. The main tests are the six transient fault tests, Nos. 1.1–1.6, with different types of fault, beginning with a single line-to-earth fault 13 miles away of 0·62sec duration at full load and ending with a close-up 3-phase fault of 0·32sec duration also at full load. One of the transient tests was made without the voltage regulator and the others with it. Pole slipping occurred in two tests (Nos. 1.2 and 1.5). In test No. 2 a 3-phase 132kV fault on the generator isolated from the system was added to the original programme in order to separate the effects of additional losses during the faults.

Tests Nos. 3.1–3.3 were made to obtain information on the transient performance with sudden insertion of 150 miles of 132kV line between the test generator and system at different loads, with and without the voltage regulator.

The preparations for the tests included, not only the detailed planning of the programme and instrumentation, but also the studies on the network analyser mentioned in the Introduction.

The basic data of the test generator, its transformer and the lines are given in Table 3.

Table 3

BASIC DATA

Generator rating	56·3MVA; 45MW; power factor = 0·8; 11·8kV, $n = 3000$, with standard form of damper winding
Synchronous reactance,* X_d ..	192% on 56·3MVA base (4·73 ohm)
Transient reactance,* X_d' ..	(a) 17·4% at 100% voltage (b) 19·2% at 50% voltage
Sub-transient reactance,* X_d'' ..	(a) 12% at 100% voltage (b) 13·9% at 50% voltage
Negative-sequence reactance,* X_2	16%
Zero-sequence reactance,* X_0 ..	6·83%
Potier reactance, X_P	17·4% at 100% voltage
Positive-sequence armature resistance, R_1	0·24% at 20°C 0·29% at 75°C
Negative-sequence armature resistance,† R_2	3·7%
Direct-axis transient open-circuit time-constant,‡ T_{d0}'	9·9sec at 100% voltage 10·1sec at 50% voltage
Saturation characteristic ..	Fig. 12
Short-circuit ratio*	0·6
Inertia constant,* H	5·85kWs/kVA
Field current at rated load* ..	324amp
Performance chart	Fig. 16
Exciter type	Motor driven
Rated voltage	315 volts
Maximum voltage	440 volts
Voltage regulator	Normally inactive, type VS.4
Transformer rating	54MVA
Transformer ratio	11·8kV delta/134kV ± 10% star in ±7 steps
Transformer reactance, based on constant h.v. current corresponding to 54MVA at 134 kV-tap*	14·76, 13·82, 12·98, 11·9, 9·95%
On tap	147·4, 143·5, 139·7, 134, 120·6kV
Transformer d.c. resistance on 24°C, h.v.*	0·908, 0·835, 0·78 ohm per phase
l.v.*	0·01368 ohm between two terminals of the delta
132kV Grid lines:	
Positive sequence impedance ..	$0·24 + j0·66$ ohm/mile
Zero-sequence impedance ..	$0·55 + j1·64$ ohm/mile single circuit $0·85 + j2·45$ ohm/mile per circuit on double-circuit lines, when both circuits operate
Zero-sequence impedance line AI–C–AII (Fig. 3)	$0·24 + j0·83$ ohm/circuit-mile
Charging current	0·36amp/mile

* Derived by test.
† Derived from a single-phase test at approximately 50% current.
‡ Calculated from test data.

(5) RESULTS OF TRANSIENT TESTS

(5.1) Records of Fault-Throwing Transient Tests on the System

(5.1.1) Summary of Results.

Readings of system conditions taken before each test are given in Table 4.

Table 4

LOAD CONDITIONS AT CLIFF QUAY 'TEST' AND 'SYSTEM' BUSBARS AND ADJOINING SYSTEM BEFORE THE TRANSIENT FAULT TESTS

Observations	Test number						
	1.1 (LG)	1.2 (LL)	1.3 (LL)	1.4 (LL)	1.5 (LLL)	1.6 (LLL)	2 (LLL)
Generator 2:							
I_{rot}, amp	256	260	266	262	214	—*	130
V_{rot}, volts	212	213	221	221	178	217	110
I_{ee}, amp	4·1	4·2	4·2	4·2	3·5	4·2	2·4
V_{ee}, volts	17·4	18·2	18·3	17·8	14·4	17·9	10·7
δ_1, deg	44	46	—	—	—	—	—
δ_2, deg	—	—	53	53	40	54	—
V_{syst}, kV	133	132·2	135	134·7	133·2	133·5	—
V_{test}, kV	145·8	146	149	148·5	145	147·5	134
P_{11kV}, MW	45·4	45·3	45·6	45·4	30·2	45·4	—
I_{11kV}, amp	2130	2130	2110	2090	1460	2120	—
V_{11kV}, kV	12·38	12·42	12·62	12·6	12·3	12·52	12·0
Q_{11kV}, MVAr	7·92	9·66	9·65	9·48	7·2	9·72	—
P_{3kV}, MW	0·25	0·248	0·258	0·256	0·234	0·254	—
Q_{3kV}, MVAr	0·321	0·33	0·358	0·354	0·319	0·344	—
I_{3kV}, A	64	80	83·2	80·8	72	80	—
V_{3kV}, kV	3·16	3·18	3·22	3·22	3·14	3·19	3·1
Transformer tapping	11·8/143·5	11·8/143·5	11·8/143·5	11·8/143·5	11·8/143·5	11·8/143·5	11·8/134
Generators 3, 4 *and* 6:							
MW Gen. 3	27·6	22·4	27·5	27·7	32·0	27·4	—
Gen. 4	28·6	22·5	28	28·4	31·3	27·0	—
Gen. 6	27·8	—	28	28·2	31·5	27·5	—
MVAr Gen. 3	13·1	14·6	22·5	23·5	26·0	26·3	—
Gen. 4	12·2	17·2	20·3	20·8	28·0	23·6	—
Gen. 6	14·9	—	20·8	20·8	23·5	24·3	—
Lines:							
Local load A	26	74	77	75	91	90	—
Cliff Quay—L A	270	190	218	220	153	146	—
Cliff Quay—DII and E A	197	132	248	252	340	322	—
Cliff Quay—G and F A	63	32	82	80	122	122	—

* During Test No. 1.5 the instrument was damaged.

The full results of the tests are given in Reference 8 and are summarized in Table 5. The first section of the Table, rows 1–6, describes the fault type and duration, the generator test conditions (load and whether the voltage regulator was working). The second section of the Table, rows 7–9, gives the maximum fault-clearance time determined by the network analysers. The third section, row 10, states whether the performance was stable or not. The fourth section, rows 11–16, gives the main readings taken before the test with respect to the test generator, and the fifth section, rows 17–19, gives the number of generators working in parallel on the 'system busbar' and other load.

(5.1.2) **Single Phase to Ground Fault** (Test 1.1).

This fault on the line 13 miles away was of 0·62 sec duration, and the test generator, on full load and with the voltage regulator in commission, remained stable as was expected both from network-analyser and the *micro-réseau* studies.

(5.1.3) **Close up Two-Phase Fault** (Test 1.2).

This test was carried out with the generator at full load and with the voltage regulator in service, and its duration was 1·28 sec. The duration of the fault had been chosen because studies on the *micro-réseau* had suggested that the system would remain just stable with a fault time of 1·32 sec. On the network analyser it was a borderline case, for, with the given data, t_{max} was found to be equal to infinity, whereas with slightly (5%) reduced generator e.m.f. it was 1·15 sec.

In the full-scale test, instability occurred in a most interesting way. Referring to Fig. 4 it is observed that immediately after the fault the generator slipped forward two pole pairs, the first full turn ending about 1·9 sec and the second about 2·4 sec after commencement of the fault. The governor of the machine reduced the steam input nearly to zero. When the machine tried to pull into step again it slipped one pole pair, this time backwards, and reached the state of swinging round the new equilibrium about 4·3 sec after the commencement of the fault. The oscillograph record of the power angle in this case was taken between the rotor and 132 kV test bar, so that the total angle between rotor and stator bar was determined by addition of the line angle obtained by triangulation from records of the difference between the two 132 kV busbar voltages. This process is naturally inaccurate, and it was decided, therefore, to record directly the power angle between rotor and 'system busbar'.

(5.1.4) **Close-up Two-Phase Faults** (Tests 1.3 and 1.4).

Both tests were taken on full load; the first with voltage regulator (test No. 1.3) had a duration of 0·96 sec, and the other (test No. 1.4) without voltage regulator was 0·98 sec duration. These tests were made in order to obtain information on the effect of the voltage regulator which will be discussed in Section 5.3.3. In both tests the generator remained stable.

(5.1.5) **Close-up Three-Phase Fault** (Test 1.5).

The test was carried out at two-thirds full load with the voltage regulator in commission, and its duration was 0·68 sec. The time chosen in this test was about halfway between the maximum fault clearing times determined by the network analyser (0·51 sec) and by the *micro-réseau* (0·9 sec). It is seen from Fig. 5 that

Table 5

TRANSIENT FAULT TESTS ON THE SYSTEM

		1.1	1.2	1.3	1.4	1.5	1.6
1. Test number		1.1	1.2	1.3	1.4	1.5	1.6
2. Fault		LG	LL	LL	LL	LLL	LLL
3. Distance, miles		13	0	0	0	0	0
4. Load, MW		45·4	45·3	45·6	45·4	30·2	45·4
5. Reactive power, MVAr		7·92	9·66	9·65	9·48	7·2	9·72
6. Voltage regulator		with	with	with	without	with	with
7. Fault time, sec	Test	0·62	1·28	0·96	0·98	0·68	0·32
8. Fault time, sec	Network analyser t_{max}	∞	1·15†	—	—	0·51	0·36
9. Fault time, sec	Micro-reseau	∞*	1·32	—	—	0·9	0·4
10. Test performance		stable	unstable	stable	stable	unstable	stable
11. I_{rot}, amp		256	260	266	262	214	258
12. δ to		test busbar	test busbar	system busbar	system busbar		
13.		44	46	53	53	40	54
14. V_{syst}, kV		133	132·2	135	134·7	133·2	133·5
15. V_{test},‡ kV		145·8	146	149	148·5	145	147·5
16. V_{11kV}, kV		12·38	12·42	12·62	12·6	12·3	12·52
17. Other generators	Number	3	2	3	3	3	3
18. Other generators	MW	84	45	84	84	95	82
19. Other generators	MVAr	40	32	64	65	78	74

* For fault at generating station stability would be maintained with voltage regulator and field forcing; without them, t_{max} would be between 1 sec (stable) and 3 sec (unstable).
† Borderline case: with given system data $t_{max} = \infty$ with slightly (5%) less generator e.m.f. $t_{max} = 1 \cdot 15$ sec.
‡ 11·8/143·5 kV tap.

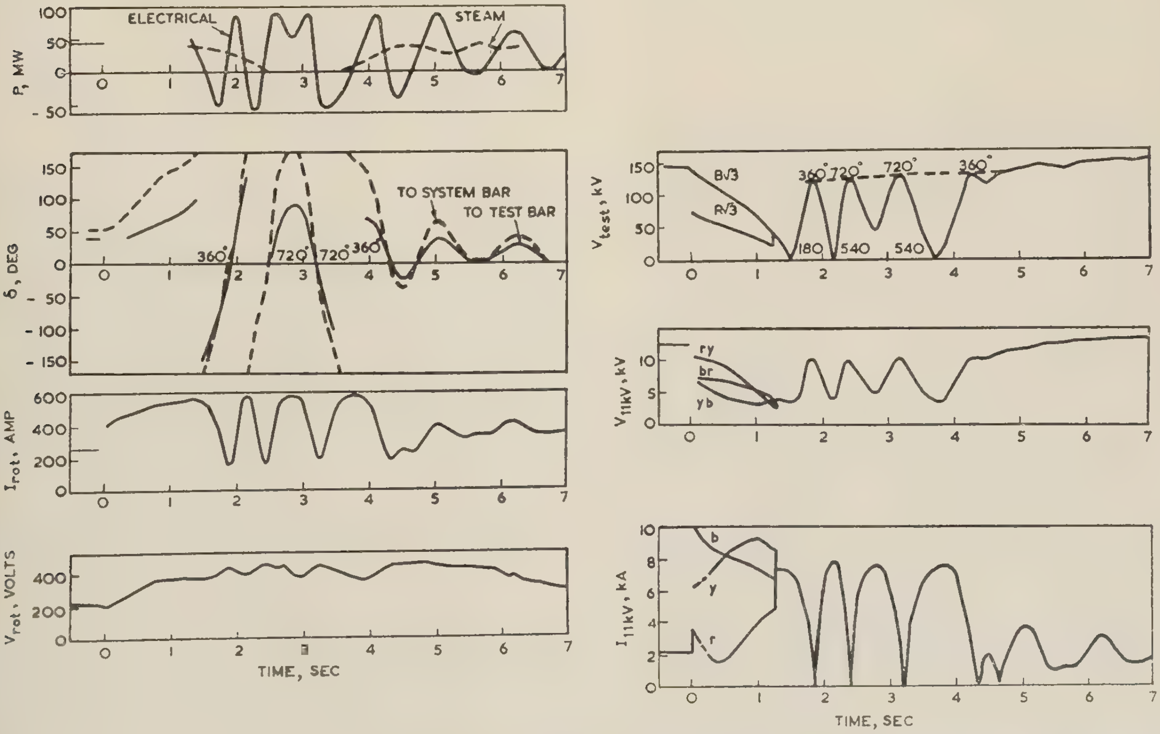


Fig. 4.—Records of Test No. 1.2.

Close-up phase-to-phase fault for 1·28 sec.
Voltage regulator in commission.

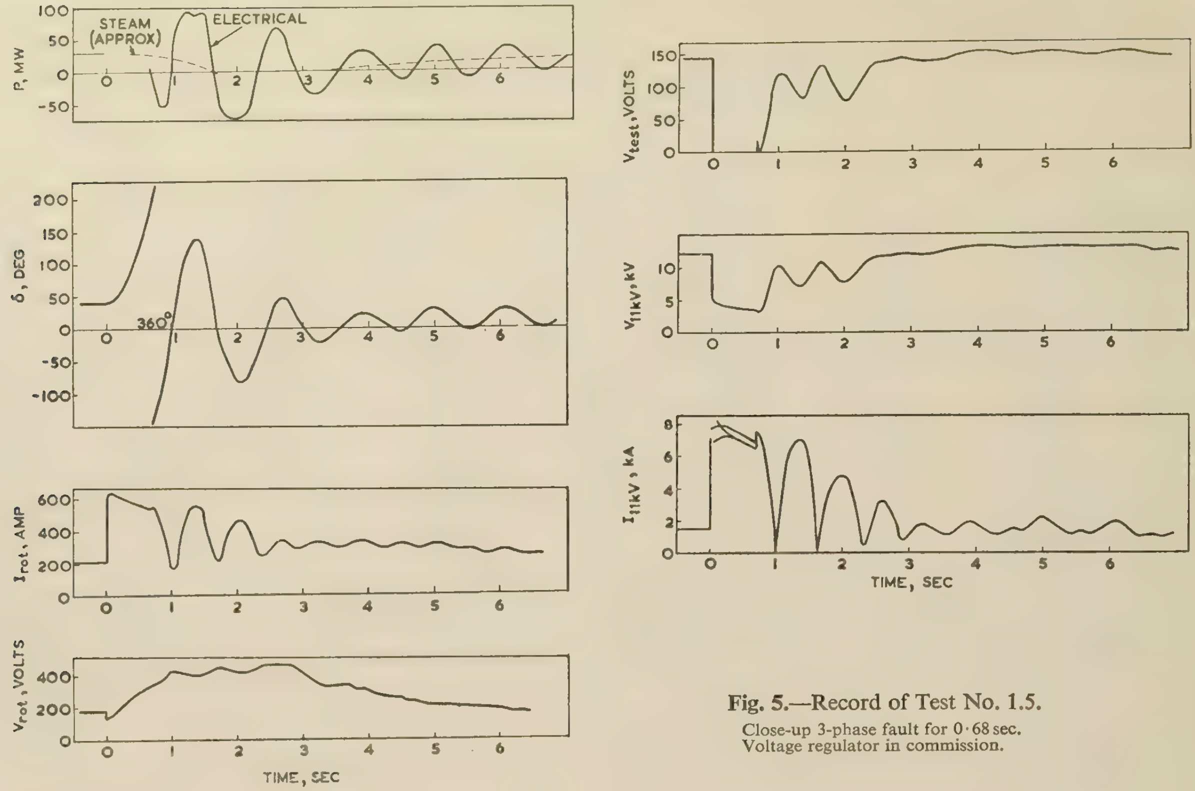


Fig. 5.—Record of Test No. 1.5.
Close-up 3-phase fault for 0·68 sec.
Voltage regulator in commission.

the generator was unstable, slipping one pole pair forward and then reaching one full turn about 1 sec after commencement of the fault. Then the generator pulled in, swinging with rather large amplitudes.

(5.1.6) **Close-up Three-Phase Fault** (Test 1.6).

The test was carried out at full load, with voltage regulator in commission, and the duration was 0·32 sec. This time was somewhat below the limit determined by the network analysers (0·36 sec) and *micro-réseau* (0·4 sec). It is seen from Fig. 6 that the generator was stable.

(5.2) Record of Fault-Throwing Transient Test on the Isolated Generator

In order to obtain some information on the losses recorded during a fault on an actual machine, and since it was realized that these data were of considerable importance in transient-stability studies, test No. 2 was added to the original test programme. The fault applied was a 3-phase fault of 0·94 sec duration without the voltage regulator in service, at rated voltage and frequency and at tap 11·8/134 kV.

The results of the tests are shown in Figs. 7 and 8. The losses which occurred during the fault were obtained by differentiating the power-angle record twice. These losses were in the beginning about twice the I^2R_1 losses calculated from the resistance of the machine and transformer windings and measured short-circuit currents, and towards the end of the test they were still slightly higher.

As this test was added to the programme on site, there was not enough time for adequate preparations. The method of obtaining the losses from the power-angle records by differentiating twice does not give the losses with great accuracy, but the final slip in Fig. 8, and thus the energy, is known to within 10%. The power/time curve in Fig. 8 may contain a larger error, and it is not impossible that the losses in the first few cycles are, in fact, considerably higher than it would appear from this Figure, which would slightly improve stability conditions for faults of short duration.

(5.3) Comments on the Results of the Transient Tests

(5.3.1) **Relationship between Power and Power Angle and the Effects of Flux Decay.**

Fig. 9 shows the power plotted against power angle from the wattmeter and power-angle recordings taken in tests Nos. 1.2, 1.4 and 1.6. For comparison the upper dashed line was added, calculated on the assumption that the voltage behind the transient reactance remains constant. The measured power is considerably smaller, which is mainly due to flux decay during the fault. The voltage behind the transient reactance was not measured directly, but the terminal voltage for the first swing at the instant when the current reaches a minimum about zero is approximately equal to this quantity. Values taken from the voltage records are given in Table 6. Comparison of this voltage with that behind the transient reactance from the vector diagram

Table 6

DECAY OF VOLTAGE BEHIND TRANSIENT REACTANCE DURING FAULTS

Test number	V_t/V_n before	V_t before	V at first zero	ΔV_t	Fault duration	Time to first zero	Note
	%	kV	kV	%	sec	sec	
1.1	111	13·1	(12·4)	5	0·63	1·05	$I_{min} \gg 0$
1.2	111	13·1	10·4	20–21	1·28	1·85	
1.3	111	13·1	11·7	11	0·96	1·35	
1.4	111	13·1	10·4	20·21	0·98	1·55	
1.5	108	12·8	10·2	20	0·68	1·0	
1.6	111	13·1	11·1	15	0·32	0·98	
2	100	11·8	7·7	35	0·94	0·94	

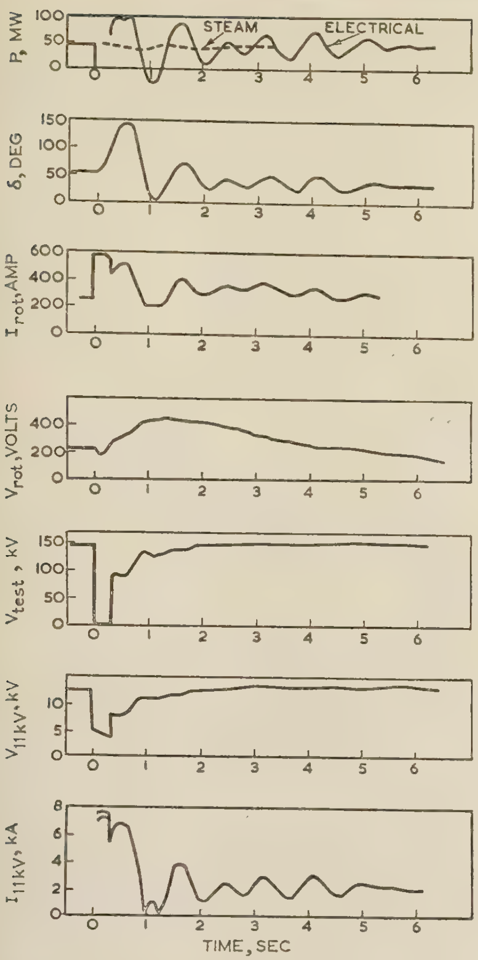


Fig. 6.—Record of Test No. 1.6.
Close-up 3-phase fault for 0·32 sec.
Voltage regulator in commission.

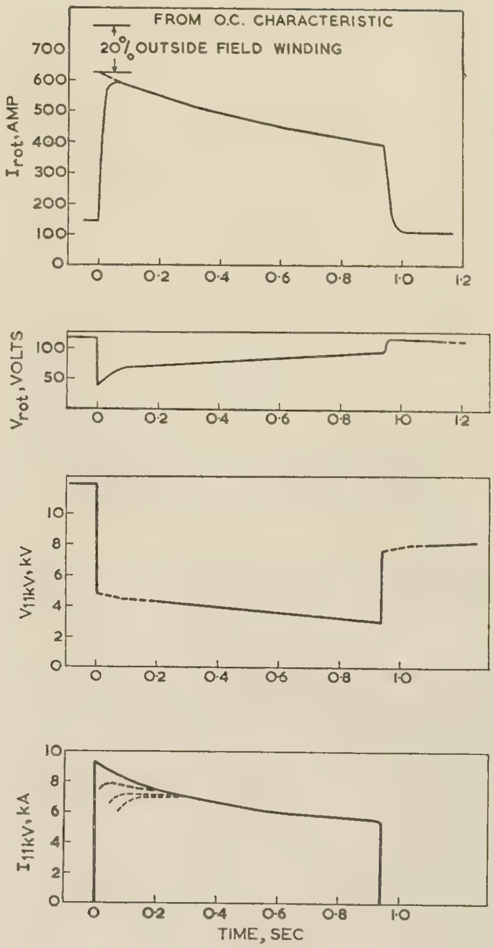


Fig. 7.—Record of Test 2.
3-phase fault on isolated generator for 0·94 sec.
Without voltage regulator.

ır the corresponding load gives the flux decay ΔV_t. These ·lues have been plotted against fault time in Fig. 10. In this ;gure the full lines give the approximate flux decay charac- ·ristic for tests without voltage regulator and the dashed line ır those with voltage regulator. This Figure does not take ıto account that the first current minimum, except in test No. 2, ; some time after the end of the fault, as will be seen from ıble 6. However, the general appearance of the curves in ıg. 10 suggests that the flux changes between the end of the ıult and the first current minimum are small because of the ınger time-constant and lower upper limit. Modified calcula- ·ons of power against power angle have been made using ·duced voltages behind transient reactance from Table 6 after ·e fault, which are much nearer to the observed values.

.3.2) Damping.

Phase shift between the branches of the power/power-angle ·r·es for rising and falling power angles contribute to the ·mping of the electro-mechanical oscillations. The total ·mping, which also contains a contribution from the prime- ·ver–governor system, can be estimated from the amplitude ·r·tion observed after the faults. Since the voltage at the ·st·m busbar swings at a slightly different frequency from that ·t·e test generator, the oscillations between the latter and the ·m busbar contain a beat which makes the initial damping ·ar larger than the effective damping really is; for example,

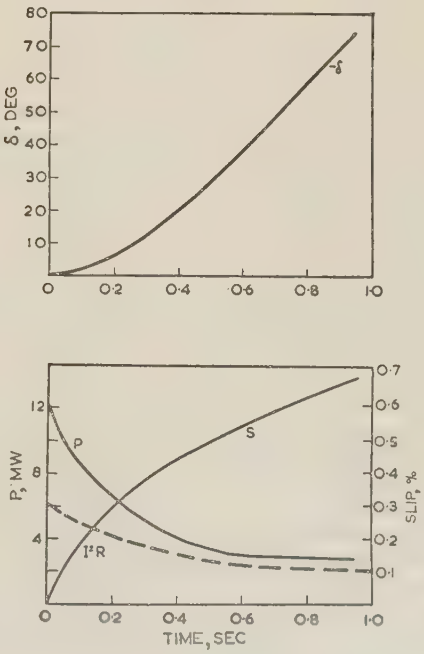


Fig. 8.—Rotor angle, slip and losses during separate short-circuit Test No. 2.

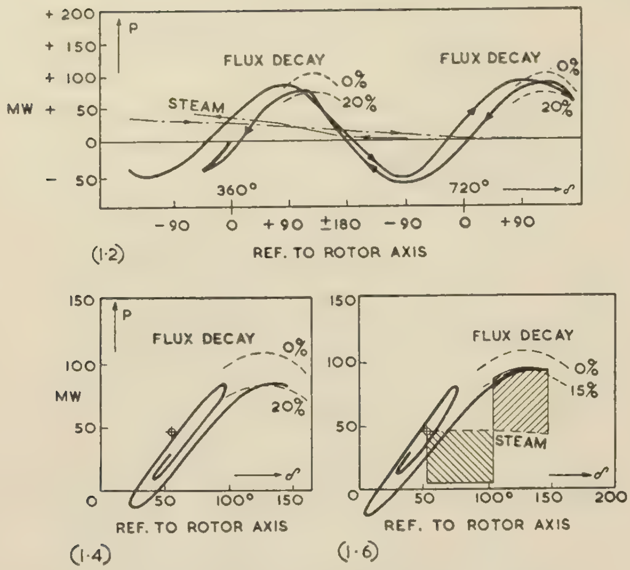


Fig. 9.—Power/power-angle curves obtained from results of Tests 1.2, 1.4 and 1.6.

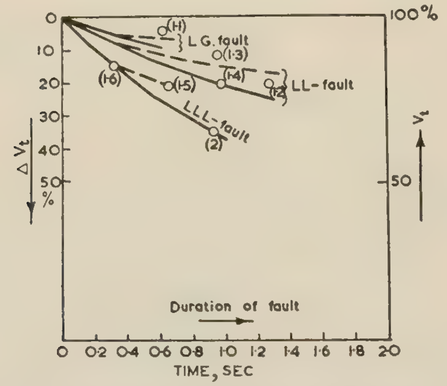


Fig. 10.—Decay of voltage behind transient reactance.

———— Without voltage regulator.
— — — — With voltage regulator.

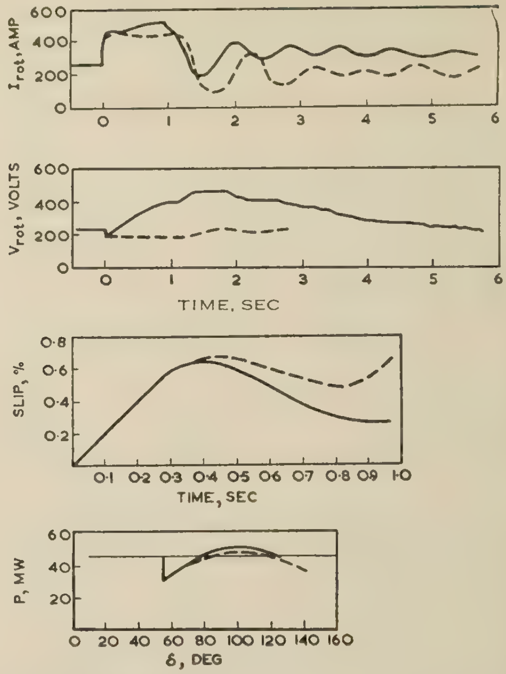


Fig. 11.—Comparison of phase-phase fault tests with and without voltage regulator.

— — — — Without voltage regulator.
———— With voltage regulator.

in Fig. 6 amplitudes at 4 sec are larger than at 2·5 sec after the commencement of the fault. For this reason only an approximate figure can be given for the effective damping.

For the distant single phase-to-earth fault (test No. 1.1) the damping was relatively good: time-constant, $t_d = 2{\cdot}16$ sec, damping constant, $T_d = 4H/t_d = 10{\cdot}8$, and equivalent slip for full-load torque, $s_e = 9{\cdot}2\%$.

For the close-up phase-to-phase fault and 3-phase faults with the voltage regulator in commission, the voltage damping was smaller, corresponding to about $t_d = 2{\cdot}5$ sec, $T_d = 9{\cdot}4$ and $s_e = 10{\cdot}6\%$.

It is interesting to note that, in all the fault tests at similar amplitudes when δ_2 remains positive, the maxima and minima of the rotor current coincide with δ_{2max} and δ_{2min}, i.e. with zero slip. The contribution of the direct-axis field winding to damping is therefore due to flux changes brought about near δ_{2max} and δ_{2min}, having the effect that the machine has a slightly larger flux when moving forwards and slightly smaller flux when moving backwards.

A calculation of the negative-sequence resistance R_2 from power-angle changes during the phase-to-phase faults, caused by the decrease between mechanical input and electrical output (including losses), is bound to be inaccurate. The values obtained from the power/angle curve in Fig. 11 normally agree with thos calculated for the positive-sequence component with due accou taken of flux decay and leave very little for the negative-sequenc component. The 100 c/s component in the rotor current recorde during the phase-to-phase faults (i.e. tests Nos. 1.2–1.4), only of the order of 20 amp (r.m.s.). This causes losses in th rotor winding of about 0·3 kW, which make a negligible co tribution to the torque.

A more direct method of determining R_2 would be a phas to-phase fault test following open-circuit operation and com paring the losses with those obtained in test No. 2, which wa under the same condition.

(5.3.3) **Effect of Voltage Regulator.**

After the transient-fault test on the system with voltag regulator, the machine retains a higher power amplitude tha without voltage regulator, but it also finds its new equilibriu in the first few seconds after the fault at a power angle δ_2 wi respect to the rotor axis which is smaller than that before th fault [Fig. 9(1.6)]. This is plausible, since the voltage regulat causes an increase in field current for a few seconds which buil up in the direct-axis flux, while the quadrature-axis compone reaches its steady-state condition much more slowly because n aid is given from the rotor. Without the voltage regulat [Fig. 9(1.4)] both components recover equally slowly, so that th equilibrium power angle immediately after the first swing is larger power angles than in the normal steady-state conditi corresponding to the reduced magnitude of voltage behi transient reactance. The curves I_{rot}/time (in Fig. 11) give direct comparison of the rotor current of the two tests, Nos. 1 and 1.4. In the curve of V_{rot}/time with voltage regulator it seen that the rotor voltage rises at the rate of 280 volts/s From a normal excitation of 324 amp, which corresponds to rotor voltage of 273 volts, to the ceiling of 440 volts, it wou take (440–273)/280 = 0·6 sec, giving an exciter response 280/273 = 1·02 per unit voltage per second.

The two lower diagrams of Fig. 11 show slip against time, and electric power (including losses during the fault) against power angle as obtained from the power-angle changes during tests Nos. 1.3 and 1.4. The curve with voltage regulator has a slightly higher power maximum, but, since the slip does not return to zero, the longer duration of this type of fault leads to instability, as was observed in test No. 1.2.

(5.3.4) **Governor Action.**

A film record taken by the Eastern Division and analysed by the London Division of the C.E.A. shows that governor movement begins about 0·3 sec after the beginning of either small or large disturbances. However, it appears from the dotted lines in the diagrams, e.g. Fig. 6 of power plotted against time, derived from measurements of mechanical power input of the turbine that the mechanical input during the first swing in which maximum power angle is reached does not change by more than about 5% of the full-load value. This is probably due to the time duration between steam input and the development of mechanical power. Behaviour in this respect is the same in all tests, but when pole slipping occurs, as for example in test No. 1.2 (Figs. 4 and 9), the governor is very effective in restoring synchronism. Not only did the governor then shut the valve completely when the machine ran above synchronous speed from about 1 to 2·5 sec after the beginning of the fault, but it also opened the valve again immediately the machine began to run below synchronism in the fourth and fifth second after the beginning of the fault. During test No. 1.5 (Fig. 5), when the governor cut the steam off again when one pole pair slipped during the first second of the fault, the input was raised gradually about 2 sec later.

It is concluded, therefore, that in network-analyser calculations of the first swing, governor action can safely be neglected. For consideration of normal damping of oscillations following transient faults some knowledge of the characteristics of the governor of the system prime mover at the natural frequency of these oscillations (about 1 c/s) would be valuable.

For resynchronization of steam turbo-alternators, the governor seems to be the most important factor.

(5.3.5) **Comparison of Actual Machine Performance with Results of Network-Analyser and Micro-Réseau Studies.**

There are two groups of factors causing results of network-analyser studies (with the usual simplifying assumptions) to differ from actual performance; one making the actual performance better, the other making it worse, than theoretical forecasts. These may be illustrated by applying the equal-area theorem to the results of the 3-phase fault in test No. 1.6 (Fig. 9).

Although this theorem is rarely applied directly on the network analyser, it is a useful means for obtaining an approximate evaluation of the error caused by the individual simplifications. It should, however, be borne in mind that a given error in terms of 'equal area' means a considerably smaller error in fault clearing time. Thus, with the conditions given in Fig. 9(1.6), 10% difference in 'equal area', i.e. between the area during and that after the fault, requires only an adjustment of the displacement angle during the fault proportional to the ratio of the accelerating power at the end of the fault (40 MW) to the sum of this power and the retarding power immediately after the fault (40 + 40 MW). In this case the ratio is 0·5, and the 5% adjustment of displacement angle is obtained by a 2·5% adjustment of the fault clearing time, because the angle is practically proportional to the square of the time.

The order of magnitude of the various errors is given in Table 7.

Table 7

ORDERS OF MAGNITUDE OF VARIOUS ERRORS IN NETWORK ANALYSER STUDIES

Effect	MW	Percentage error of equal area	Percentage error of fault clearing time
		%	%
Effects improving actual performance			
(i) Additional loss during fault ..	3	8	2
(ii) Reduced input through governor action after the fault, average	1·5	3	0·75
(iii) Damping, average	4·5	9	2·25
(iv) Total error due to effects improving actual performance ..	—	20	5
Effect worsening actual performance			
(v) Flux decay	20	40	10

Transient-stability investigations with the usual simplified assumptions are thus, in this particular instance, slightly on the unsafe side, by about 5% in fault-clearing time. The maximum permissible fault-clearing time in the present investigation was calculated as 0·36 sec, and the practical value would be 5% smaller, or 0·34 sec. This accuracy is sufficient in almost every practical case. On those rare occasions when operation very near the stability limit cannot be avoided, an occasional check on the usually neglected factors may be advisable.

In the preliminary studies of the transient tests on the *micro-réseau* the additional losses during short-circuit were rather more emphasized. Damping and governor action also did not seem to have been correctly represented owing to the fact that reliable data on the latter were not available.

The transformer ratio actually used during the tests was not known during the preliminary studies, but inaccuracies from different tapping ratios are not of great consequence, since they bring about a change both in reactance and voltage which, to a considerable extent, cancel out.

(5.3.6) **Transient and Sub-transient Reactance.**

From the results of test 2 with a 3-phase fault following open-circuit operation, it has been possible to calculate transient and sub-transient reactance.

By plotting the logarithm of current against time the following values are obtained:

	Sub-transient	Transient
$t = 0$ with $I_n = 2{\cdot}75$ kA with $X_t = 12{\cdot}4\%$	$I_d'' = 9{\cdot}5$ kA $X_d'' + X_t = 28{\cdot}9\%$ $X_d'' = 16{\cdot}5\%$	$I_d' = 8{\cdot}2$ kA $X' + X_t = 33{\cdot}6\%$ $X_d' = 21{\cdot}2\%$

These values of X_d' and X_d'' agree with those given in Table 3 to within the accuracy limits of such measurements.

The transient reactance may also be obtained from voltages at the in-phase position and current maxima at phase opposition in test No. 1.2 where pole slipping occurred. This calculation is given in Table 8. It is seen that the values for X_d' agree well with the results from works tests given in Table 3.

Since generator transient reactance is only about one-third of the effective reactance between generator e.m.f. and system busbar, a tolerance of 10–20% in this value would introduce errors of only 3–6% in the amplitude of power-power angle

Table 8

CALCULATION OF TRANSIENT REACTANCE FROM VOLTAGE AND CURRENT RECORDS (TEST 1.2)

t	I_{test}	V_{11kV}	V_{syst}	Z	X	$(X_t + X_l)$	X'_d
sec	%	%	%	%	%	%	%
2·12	278	89	78·5	60·1	59·1	40·5	18·6
2·8	279	89	76·0	59·4	58·4	40·5	17·9
3·9	268	89	75	61·1	60·1	40·5	19·6

curves, 6–12% in 'equal area', or 1·5–3% in fault clearing time. These errors appear to be tolerable in the light of the findings of Section 5.3.5.

(5.3.7) **Fault Current.**

Table 9 gives the fault currents for the six fault tests with a test generator feeding into the system through the 150-mile line. These agree with calculated values. For the sharing of the zero-sequence current in test No. 1.1 between the test generator transformer and the line, it should be remembered that the zero-sequence impedance of this line of 150 miles is unusually low because the current passes down one circuit and returns on the second circuit of the same double-circuit line.

(5.3.8) **Miscellaneous.**

The direct-axis transient short-circuit time-constant determined from test No. 2 for a 3-phase short-circuit at the 132 kV terminals is 1·5 sec. This agrees well with the rate of flux decay of test No. 1.6 (Fig. 10), for a similar short-circuit following full load (45 MW), considering that the steady-state flux for this short-circuit, corresponding to the excitation of the load, is 31% of the value for the e.m.f. behind transient reactance before the fault.

The transient component of the field current in test No. 2 (Fig. 7) decays at the same time-constant of 1·5 sec.

The current required in the field winding to maintain the original flux during the fault can be estimated by a Potier-triangle construction as shown in Fig. 12 for 100% e.m.f. and $(X_p + X_t)$ reactance. It is interesting to note that the actual peak of the transient rotor current recorded is about 20% smaller than this estimated value. This is because there is also some current flowing in the damper winding and in the rotor steel.

The transient component in the field current takes about 0·06 sec to reach its maximum, corresponding to a time-constant of about 0·02 sec.

The direct-axis sub-transient short-circuit time-constant determined from the alternating current is about 0·08 sec.

The 50 c/s component in the field current decays with a time-constant of about 0·12 sec.

The 11·8 kV voltage is distorted immediately after a sudden disturbance, such as the beginning or end of a fault. The distortions decrease with a time-constant of about 0·03–0·04 sec, i.e. after 4·5 to 6 cycles they have practically disappeared. They may be due to transformer magnetizing inrush currents.

The voltage variations on the system busbars and the power-angle variations of the generator on the system busbars were of the order expected.

Fig. 13 shows the variation in the angle between the system-busbar voltage at Cliff Quay and the 'system busbar' and the voltage at the distant Grid point for test No. 1.5 (the effect of which was reported to have been widely felt throughout the country). The system phase-angle curve takes about half a minute to return to its normal shape, the initial fluctuations being of the order of ±5°. Knowledge of the magnitude and duration of this voltage and phase variation after faults is of interest in connection with problems of reclosing, and especially for determining reclosing time.

Table 9

FAULT CURRENTS

Test number	1.1	1.2	1.3	1.4	1.5	1.6
Distance, miles	13	0	0	0	0	0
Fault between phases	Y–Earth	R–Y	R–Y	R–Y	R–Y–B	R–Y–B
Fault current, Measured kA	1·4	1·28	1·33	1·25	1·45	1·5
Fault current, Calculated kA	1·33	1·27	1·31	1·31	1·47	1·5
$3I_0$ measured, kA	1·4	—	—	—	—	—
From line R, amp	80	460	500	490	680	670
Y, amp	502	640	680	690	670	650
B, amp	220	160	160	175	670	650
$3I_0$, amp	370	—	—	—	—	—

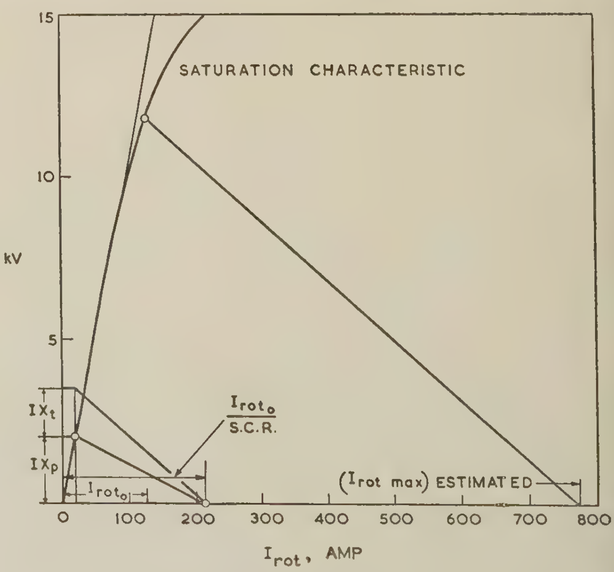
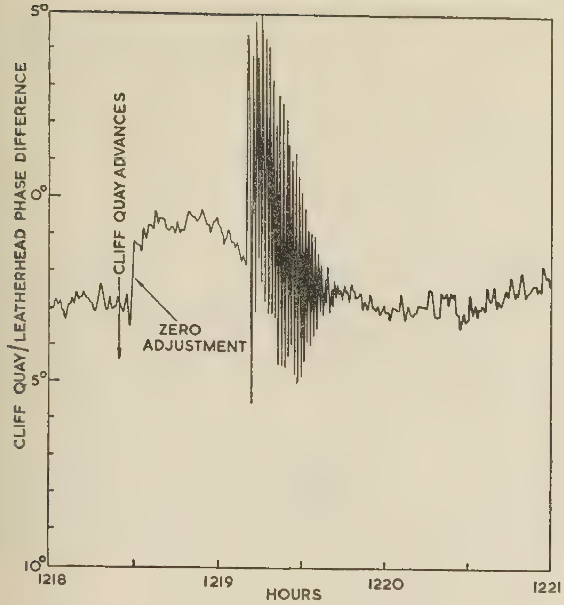


Fig. 12.—Potier construction for estimating I_{rot} max. during short-circuit.
Test No. 2 (3-phase fault, no load).

(5.4) **Transient Performance with Insertion of Line**

The object of the tests during which the reactance of 150 miles of 132 kV line was suddenly inserted between the 'test generator busbar' and the 'system busbar' by opening the



'ig. 13.—Angle between 'system busbar' at Cliff Quay and Leatherhead.

oupler was to determine whether this sudden increase of mpedance would cause instability.

Five tests were made without voltage regulator at 15 MW oad and three at 45 MW load, proceeding from tests with high xcitation to those with lower excitation, which are nearer the tability limit. The 45 MW test with the lowest excitation —9 MVAr, 70° power angle before line insertion) was repeated vith voltage regulator, and the results of the recordings are hown in Fig. 14. These are identical except that, in the ase without regulator, the rotor voltage was slightly smaller fter 1·5 sec, but the difference of only 3–6% is too small to be f any significance. It thus seems that this type of voltage egulator has a negligible effect on the small swings caused by e sudden insertion of 150 miles of 132 kV line even at an active ower of 45 MW. After attenuation of the transient swings a ew equilibrium was approached slowly, or, when the initial ower angle was too near 90°, because the excitation was too ow for the machine to be stable with the line, the machine lided slowly to angles above 90° leaving ample time (several econds) for corrective action (increase of excitation) to be taken y hand or by voltage regulator.

(6) SUPPLEMENTARY TESTS

(6.1) Steady-State Stability Tests direct on Busbars without Voltage Regulator

The tests were made in order to find the steady-state stability mit and to compare it with theoretical values and the perormance chart.[9] For different loads of 0, 15, 30 and 45 MW, e excitation was reduced in steps at which readings were taken f all voltages, currents, reactive power and power angle, until ole slipping occurred. With no load the power angle naturally emained constant near zero. For the other loads, pole slipping ccurred when a power angle of about 90° was exceeded. Even hen pole slipping had occurred the machine was never disconected from the busbars, but it pulled immediately into step fter slipping slowly over one pole pair, which gave sufficient me for taking corrective action after one pole had been slipped. ig. 15 shows the rotor current and the power angle plotted gainst reactive power for the three constant loads. Calculated

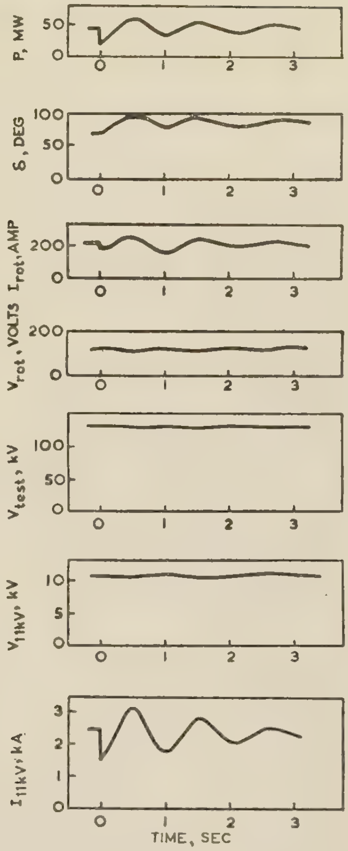


Fig. 14.—Records of Test No. 2 (sudden insertion of line). Voltage regulator in commission.

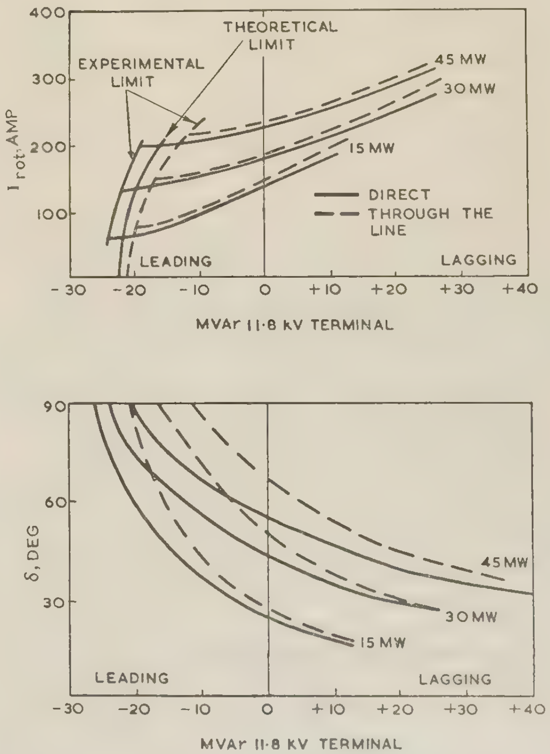


Fig. 15.—Rotor current/MVAr (11·8 kW terminal) and rotor angle/MVAr curves.

values of reactive power and rotor current at which instability ($\delta = 90°$) is expected were compared with the actual values, and it was found that the reactive power is slightly higher than calculated values based on the unsaturated synchronous reactance, which is probably due to saturation effects. Thus the practical steady-state stability conditions are better than the theoretical forecasts. Closer agreement with calculations would be obtained with the use of a synchronous reactance equal to the reciprocal of the s.c.r., or, since, at $\delta = 90°$, the reactive-power current component acts in the direction of the quadrature axis, with use of a quadrature-axis synchronous reactance of about 15% smaller than the value of unsaturated direct-axis synchronous reactance.

The machine-performance chart used in practice gives the limit of the reactive power at machine terminals for constant voltage at those terminals. Actual operation takes place with constant voltage at the 132 kV busbars, and the reactive power is usually measured on the h.v. side of the generator transformer. This, together with the increased reactive-power limit found in the tests, brings the stability limit BC in the performance chart (Fig. 16) very much to the safe side. Thus, previous tests, that the reactive power limit is about 15% higher than calculated values based on unsaturated direct-axis synchronous reactance. Again, when instability occurred, the machine moved slowly to power angles above 90° so as to leave time for corrective action to be taken in order to prevent slipping of more than one pole pair.

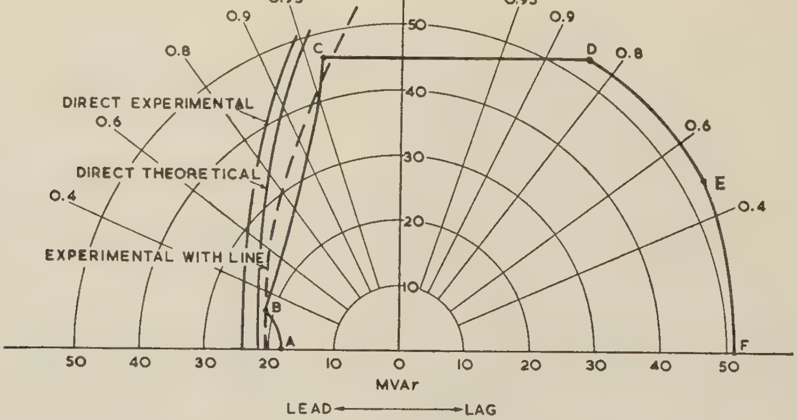


Fig. 16.—Performance chart of test generator for 95% terminal voltage.

even with the tap ratio of 11·8/143·5 kV at which instability occurred at about 86% of rated voltage, the stability limit is just outside the operating limits of the performance chart for 95% generator terminal voltage which would be available to the operator, and which includes a safety margin for a load variation of 5% of full load on the prime mover. The other limits are as follows:

AB Minimum of the excitation current control limit.
CD Maximum power of the prime mover.
DE Maximum permissible stator current.
EF Maximum permissible rotor current.

The curves on constant load in the diagram of rotor current against reactive power given in Fig. 15 are, near the stability limit, very flat, which makes operation without voltage regulator near the stability limit rather sensitive to variations in the rotor current or load.

(6.2) Steady-State Tests without Voltage Regulator including the Line

The tests were made in the same way as the previous ones, with constant voltage at the system busbars. The results are plotted in Figs. 15 and 16, and they confirm the findings of the

(6.3) Steady-State Tests with Voltage Regulator including the Line

The stability limit, i.e. operation near 90° power angle and above, was approached by a reduction of the voltage-regulator setting. It was necessary to extend the normal range of the voltage regulator by additional resistance. Even then it was very difficult to obtain instability. Whenever the power angle to the system busbar reached values of about 120° the voltage regulator 'boosted' the excitation and usually managed to pull the machine back to power angles below 90°. This is probably due to the fact that the field current at power angles near 90° produces directly active current, i.e. in phase with the busbar voltage, while usually, i.e. at small power angles, it only produces reactive current. Thus, near zero power angle, any rotor-current variations caused by the voltage regulator, which always depend on the exciter response and the generator field time-constant corresponding to the circuit conditions, affect the active power only after subsequent changes of the power angle, which depend on the mechanical inertia of the machine. But at large power

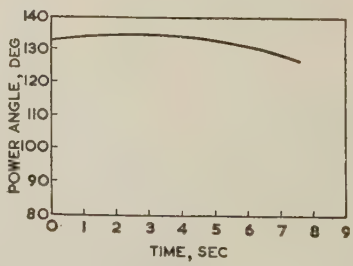


Fig. 17.—Power-angle/time characteristic of test generator on 30 MW load.

85% machine terminal voltage.

angles rotor-current variations affect power output directly without any changes of the power angle. Fig. 17 shows a case where the power angle reached values above 130° during a test

made at 85% machine terminal voltage, 30 MW load and, for example, 20·9 MVAr at $\delta = 100°$.

(6.4) Asynchronous Operation of the Test Generator with Open Field Circuit

An opportunity was taken to make some tests with asynchronous operation of the test generator with open field which may have practical importance in compensating for high system voltages and in the rapid connection of machines to the system without requiring a synchronizing operation. Tests were made at 0–30 MW with the generator loaded directly on the busbar, and at 10 MW only for supply through 150 miles of line to the system busbar. Voltage, current, reactive power and slip are plotted against active power in Fig. 18 with the points for 10 MW

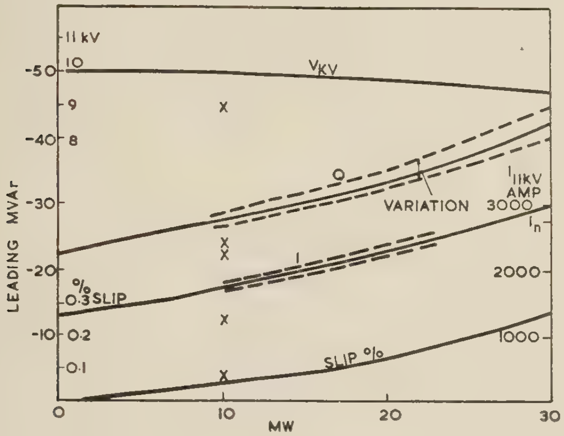


Fig. 18.—Asynchronous performance of test generator with open field.
——— Direct on busbars.
× 10 MW through line.

operation through the line marked separately. The reactive-power input showed a slight variation during the slipping of the rotor, owing to differences in magnetic reluctance of the rotor in the direct and quadrature axis. If the rotor were replaced by a steel cylinder without slots for a field winding, this variation of reactive power would disappear. The slip is extremely low; since the field circuit was open, the rotor current corresponding to the torque must have found a very low-resistance path in the damper winding and the steel. Unfortunately this low-resistance path for the rotor current is not available for damping after transient faults because the penetration of alternating current in the steel depends on frequency (see Table 10).

The maximum of the power/slip curve was not reached at 30 MW and no further attempt was made to determine it. If operation in this condition continues for any length of time the temperature in the rotor and stator should be watched.

Table 10

Depth of Penetration of Alternating Current into Steel (Resistivity $12 \cdot 5 \times 10^{-6}$ ohm-cm, Permeability = 1 000), for different Frequencies

Frequency	Slip	Penetration	Case
c/s	%	in	
0·028	0·055	1·4	Asynchronous operation at 10 MW
0·13	0·27	0·6	Asynchronous operation at 30 MW
1	2	0·23	After-fault oscillations
50	100	0·032	Rotor current caused by d.c. transient in stator
00	200	0·023	Rotor current caused by negative-sequence stator current

(7) CONCLUSIONS

The results of full-scale stability tests carried out on a 56 MVA cylindrical-rotor turbo-alternator are, in principle, in good agreement with analytical results obtained prior to the tests.

The results of network-analyser studies of transient stability employing the usual simplifying assumptions are found to be reasonably accurate, although they show the system to be slightly more stable than in practice. It was found, however, that during the transient fault tests, stability was maintained within the time limits determined by the network-analyser studies.

Errors are only about 5% of the permissible fault-clearing time, which should be tolerable in almost every practical case; where operation is very near the stability limit a check on the effects of factors usually neglected may be advisable.

The effects of saliency on transient reactance were not so material as to be observed.

A tolerance of 10–20% in calculating the transient reactance introduced errors into the power/power-angle curves of 3–6%, and 1·5–3% in fault clearing time, which are not considered to be excessive.

The action of the voltage regulator used during the tests gave results which were noticeably beneficial in faults lasting more than 0·3 sec.

Governor action can safely be ignored for the first swing of swing curves, but it is very helpful for subsequent resynchronization of turbo-alternators.

The fault currents observed during the tests are in agreement with calculated values.

Steady-state stability at Cliff Quay was better in practice than in theory because of saturation effects.

The voltage regulator greatly improves steady-state stability. It was almost impossible to obtain instability with the voltage regulator in commission. When the rotor moved slowly to a power angle of about 120°, the voltage regulator usually pulled it back below 90°.

Short-time asynchronous operation of the test generator at Cliff Quay with open field either direct on busbars or through 150 miles of 132 kV line was possible with a very small slip up to 30 MW. The test limit was set by fear of possible damage to the machine due to overheating.

(8) ACKNOWLEDGMENTS

The authors are indebted to the Director of the Electrical Research Association and to the Chief Engineer of the Central Electricity Authority for permission to publish the paper. They gratefully acknowledge the help and encouragement received from the kind co-operation and happy team work of a great number of colleagues of their own and other organizations. They are indebted to the Controller and Headquarters Staff of the Eastern Division and to the staff of Cliff Quay Generating Station, for the overall planning, co-ordination and control of a test programme involving much detailed work. They are also indebted to the London Generating Division, the C.E.A. Research Laboratories, the North of Scotland Hydro-Electric Board, the Imperial College of Science and Technology, Queen Mary College, London, Metropolitan-Vickers Electrical Co., Ltd., Ferranti Ltd. and A. Reyrolle and Co., Ltd., for help with measurements; to the Royal Aircraft Establishment, Gloster Aircraft, Ltd., and the General Electric Co., Ltd., for the loan of instruments; to Associated Electrical Industries, Ltd., and Électricité de France for network-analyser and *micro-réseau*

studies, and to the Chairman and members of the E.R.A. Panel V/Da for their initiative, advice and helpful suggestions.

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[The discussion on the above paper will be found on page 366.]

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Feb. 1958

ORGANIZATION FOR LARGE-SCALE GRID SYSTEM TESTS

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(*The paper was first received* 4*th December, and in revised form* 31*st December*, 1957. *It was published in February*, 1958, *and was read before the* NORTH MIDLAND CENTRE 4*th February*, THE INSTITUTION 6*th March, and the* NORTH-EASTERN CENTRE 10*th March*, 1958.)

SUMMARY

The stability of generating plant on a large interconnected system is a very important consideration of design and operation. Small-scale tests and the development of various types of calculator have provided the machine designer and system planning engineer with useful information, but the ultimate test is that of satisfactory performance in service. Tests on an operational system are very desirable, but before such work is carried out, the operational risks must be weighed against the importance of the results to be obtained.

In 1956 it was decided that such an investigation should be made, and for the first time the Central Electricity Authority carried out steady-state and transient generator stability tests on an operational section of the system.

(1) INTRODUCTION

The paper describes the organization of a series of tests to study generator stability characteristics which were carried out in 1956 at the request of Panel V/Da (System Constants) of the Electrical Research Association and with the approval of Mr. J. D. Peattie, Chief Engineer of the Central Electricity Authority.

Cliff Quay is a major generating station with six 45 MW machines generating at 11·8 kV and switched through generator transformers at 132 kV to a double-busbar substation. 132 kV feeders radiate to Colchester in the south, Great Yarmouth in the north and Sundon in the west. Local 132/33 kV supplies are given at Ipswich, Stowmarket and Wymondley.

By suitable switching, the double-busbar arrangements at Sundon and Cliff Quay made it possible to obtain a 132 kV feeder approximately 150 miles long, starting and terminating on different busbars at Cliff Quay. This was the main reason why Cliff Quay was chosen for the tests. In addition, it afforded the facility of controlling the tests and recording the essential information at one site.

The only time suitable for carrying out the tests was during the August Bank Holiday period, when industrial supplies normally given from one of the Cliff Quay–Sundon lines at Wymondley were reduced to a minimum and the load could conveniently be transferred elsewhere. The dates chosen were Saturday to Tuesday, 4th–7th August, 1956.

(2) GENERAL OUTLINE OF THE REQUIREMENTS FROM THE SYSTEM AND THE PLANT

Tests were made in three stages, with the following aims:

(*a*) To study the steady-state stability of a generator connected to a system, both directly and through a long transmission line, with and without automatic voltage regulators. At specified loads the excitation was gradually to be reduced until pole slipping commenced. All observations were to be made visually in this stage.

(*b*) To study the transient stability of a generator, with and without automatic voltage regulators, following the sudden insertion of a high line reactance in the system. This was to be accomplished by adjusting the load and excitation of the test machine whilst in parallel with others and then suddenly inserting, by the opening of a circuit-breaker, 150 miles of line in series with the test machine.

(*c*) To study the transient stability of a generator, with and without automatic voltage regulators, coupled to the system through a high line reactance, resulting from the application of system faults. In this set of tests the machine was to be loaded to specified conditions, and 132 kV faults of varying types were to be applied. The records were to be obtained automatically in this and the preceding stage.

The task of organizing these programmes had the following major aspects:

To safeguard and maintain supplies to consumers with the minimum of disturbance.
To safeguard the system and plant under test.
To obtain the necessary test information.

(3) DETAILS OF THE SYSTEM AND ITS OPERATION AND CONTROL

(3.1) The System

The general layout is given in Fig. 1. The 132 kV busbars at Cliff Quay were operated in two sections—the system section

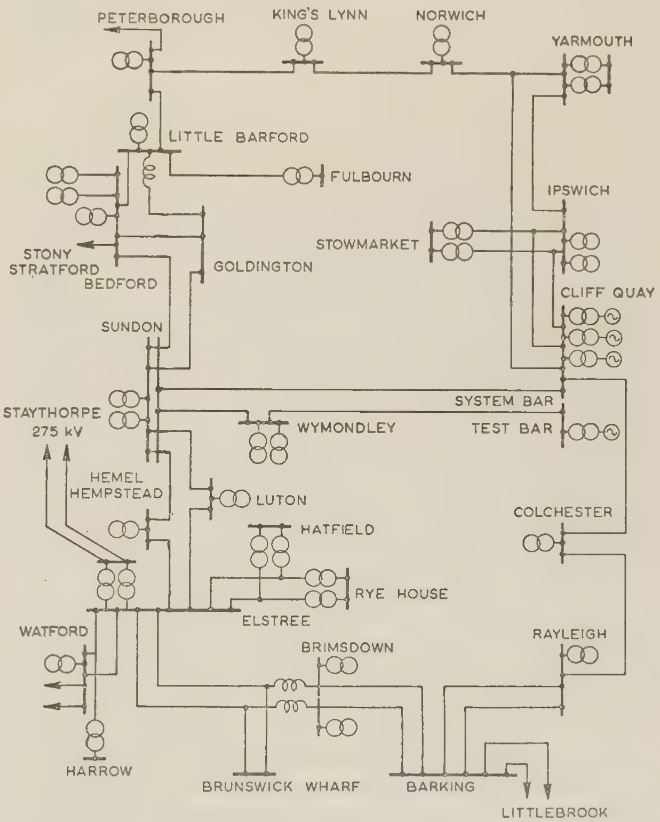

Fig. 1.—132 kV system arrangement.
All the transformers are not shown.

and the test section. To the system section were connected all local supplies and 132 kV feeders with the exception of one Sundon feeder. This was switched to the test section. One

The authors are with the Eastern Division of the Eastern, London and South Eastern Region of the Central Electricity Generating Board.

generator was selected to the test section with the remaining generators on load on the system section.

At Sundon, the Cliff Quay feeders were switched to the reserve 132 kV busbar and all other feeders and the two transformers were switched to the main busbar.

At Wymondley the two 132 kV transformers were switched to the Cliff Quay–Sundon line, but both 33 kV switches were open. The load normally supplied at 33 kV was transferred elsewhere. Should the necessity have arisen, rapid restoration of 33 kV supplies to Wymondley would have been possible.

Each evening certain steps were taken to restore the system to normal in case fault conditions should arise during the night, when testing was not in progress.

(3.2) Operation of the System

The risks to the system, which might be encountered during the tests, were considered in advance and it was decided that the worst risk of losing all the generation at Cliff Quay should be catered for. The loss of certain transmission lines was also considered, but this was thought to be a smaller risk to the system.

To cover the total loss of Cliff Quay generation, arrangements were made for generating plant to be available at all adjacent stations, and loading programmes for Cliff Quay and these stations were predetermined and the resultant transmission-line loadings assessed.

As the system load during the four days of the test period was expected to be one of the lowest of the year, and very dependent on weather conditions, the maximum loading conditions which might occur during the mornings were estimated for bad weather, whereas the minimum loading conditions, during mid-afternoons, were estimated for good weather.

Various methods of loading Cliff Quay generating station were considered, and it was decided that the station should work to a total load programme irrespective of the load carried by the test machine. Apart from the test machine, the remaining generators were to be available for normal commercial operation. This resulted in Cliff Quay being assigned a loading programme which varied from 70 to 120 MW dependent on the time and day.

It was desirable that the overall loading of Cliff Quay should be as steady as possible during each test sequence, and the timing of the tests was carefully chosen to avoid rapidly changing system conditions, particularly at midday.

System voltage conditions were considered, and it was agreed that the voltage on the system busbar at Cliff Quay should be not greater than 132 kV. This was to ensure that reasonable reactive-power loading on the test generator could be achieved with between 138 and 145 kV on the test busbar, when the two busbars were coupled by the Sundon lines. In order to give reasonable reactive-power loadings on the generators connected to the system busbar at Cliff Quay, particularly under low system load conditions, the voltage on the remainder of the 132 kV system was adjusted accordingly. Experiments were carried out prior to the tests during lightly loaded periods, to see how these results could be best achieved.

(3.3) Control of the System

The main part of the system concerned in the tests comes within the Thames North Grid Control Area of the Central Electricity Authority. The normal functions of the Grid control engineer are to ensure security of supply to all the Grid supply points, instruct generator output, control switching operations and safety requirements associated with the high-voltage transmission system, and deal with fault conditions.

In order to facilitate the test procedure a special variation of centralized Grid control was adopted. Throughout the tests a Grid control engineer, called 'the local Grid control engineer' was located in the Cliff Quay station electrical control room. To him were transferred, from the Grid control centre, the responsibilities of Grid control for Cliff Quay and Ipswich generating stations, and the 132 kV substations at Cliff Quay, Ipswich, Stowmarket, Wymondley and part of Sundon. Testing took place on each day between the hours of 0900 and 1800, and the local Grid control engineer operated during these hours. His control desk was equipped with a hand-dressed diagram of the 132 kV system directly concerned.

(3.4) Control of the Generating Plant and Precautions for its Safeguard

The station operating engineers were acquainted with the project from the outset, and several meetings were held with them to discuss the various tests required; their views were very helpful and rehearsals of the operating procedure were carried out.

The shift charge engineer at Cliff Quay was responsible for all operational aspects of the station plant including the test machine. For the purpose of the tests, however, an additional shift charge engineer, known as the 'test machine engineer', was stationed at the test machine and given responsibility for its safety. Although he was responsible to the shift charge engineer on duty, he was given the authority to trip the machine mechanically or to interrupt any test should he consider the condition of the machine to be dangerous.

It was decided that the same senior operating staff should be on duty during all the tests, in order that the experience gained during the earlier tests would be of benefit in the later, more stringent, stages.

(3.5) Special Communication Facilities

The communications were divided into two main functional requirements—external and internal to the generating station.

(3.5.1) External Communications.

The local Grid control engineer was provided on his control desk with an extension telephone on the house private automatic exchange which gave access to all points on the system under his control and to Thames North Grid Control Centre, the latter being provided with two separate private telephone circuits.

An ex-directory Post Office telephone extension and a high-frequency carrier telephone service over one of the 132 kV lines to Sundon were also installed. The carrier telephone, in addition to giving direct access to Sundon, provided an alternative route whereby messages could be relayed from Cliff Quay to Sundon and on to the Thames North Grid Control Centre in the event of the failure of normal communications.

(3.5.2) Internal Communications.

A separate internal telephone switchboard was provided to give communication with the station control room, the various testing points and the test generator. Simultaneous conversation with three or more points was possible. Silence booths were installed where background noise was high.

The station private automatic exchange was reserved entirely for the use of the operation staff involved in the tests.

The station electrical control room, which was used as the centre for controlling operations, is situated in a building separate from the turbine room. A closed-circuit television installation was provided with two cameras in the turbine room, in order to give pictures in the electrical control room, and these could be chosen to suit the test conditions. A view of the turbine governor valves on the screen was very valuable in indicating conditions on the turbine to the control-room staff.

(3.6) Control of the Tests

At an early stage those in charge of the tests were invited to attend meetings of the Panel V/Da. Arising from these discussions, a detailed programme of requirements was drawn up and agreement was reached on all the quantities that were to be recorded, both visually and automatically. It transpired that, in the transient tests, some 90 simultaneous recordings were required. After this detail was settled, site meetings were held to co-ordinate the whole programme and arrangements made for control, indication and recording.

A temporary control and indication panel was installed in the station control room. Its main purpose was to concentrate at one point the additional instruments, indications and control facilities required by the tests. Multicore cables were run from this panel to the various recording points.

(4) APPLICATION OF THE TEST CONDITIONS

For several years prior to the tests and before 132kV system reinforcement had been commissioned, occasions had arisen at Cliff Quay when generator pole slipping had occurred, and in one case a complete loss of generation had resulted. The operating staff had, over the course of time, evolved certain corrective techniques but they were naturally a little hesitant to operate machines with reduced excitation.

With this in mind, the steady-state stability tests were carried out first. Excitation was reduced in a series of gradual steps, and although this procedure was not considered altogether necessary, it enabled the operating staff to evolve a technique suitable for the test conditions. This was amply justified in the subsequent tests, when severe pole slipping took place, since, by then, the control staff had learnt to delay the natural tendency to take corrective action, and the machine was allowed to slip poles sufficiently for necessary recordings to be made. This gradual approach in the initial tests also gave the opportunity to test lines of communication, recording instruments and observers' techniques.

In the transient-stability tests the sequential operation of starting all the recording equipment and the application of test conditions was carried out by a master control switch provided by the Electrical Research Association.

Each test condition was applied 10sec after red lamps were illuminated at each recording point, at the test generator, and in the boiler house.

The pole slipping which resulted from some of the tests was a considerable strain on the electrical control-room engineer, as the normal instruments provided on a generator control panel gave a very alarming picture. This was aggravated by the knowledge that violent fluctuations in current were being produced in the other generators in the station and in feeders in the system. The most useful of the panel instruments, however, was the field ammeter, which did give an indication of whether or not the rotor was pulling into step. Later in the series of tests, a chart recorder was installed near the test generator control panel and a record was taken of the rotor angular position. When this was oscillatory, a valuable indication was given to show whether the machine was tending towards or away from stability. This instrument then became the sole criterion of the rotor performance and was used to decide whether corrective action was necessary. In order to restore stability more quickly after some of the transient tests, a rapid field-current boost was applied by remotely closing the field-forcing contactor from the test generator control panel.

In no test was it found necessary to trip off the test machine, but on some occasions corrective action, by increasing field current and/or reducing load, had to be taken in order to restore stable conditions.

(5) SPECIAL PROTECTIVE-GEAR MEASURES AND LIGHTNING FAULTS DURING THE TESTS

(5.1) Protective Gear

Most of the primary 132kV feeder protection on the system used for the tests employs directional relays, and the system voltage is one of the references. It is well known that relays of this type can operate incorrectly if one part of the system loses synchronism with another. It was therefore necessary to disconnect such protective relays during the tests, and, wherever possible, over-current relay grading was adopted. In some cases over-current protection was disconnected for the short time of the test condition. This was because the currents flowing during the test were considerably larger than those which would have flowed through the over-current relays if system faults had occurred. The disconnection of protective relays at several points before each test, and the reconnection as soon as possible afterwards, needed careful programming. All the primary protective relays were reconnected at night.

Careful consideration had to be given to the safety of the 132kV switchgear. Conditions could have arisen in which the switchgear might have been subjected to recovery voltages or to the rupture of capacitance currents in excess of those for which it was designed. These conditions were safeguarded by arranging for the simultaneous tripping of two circuit-breakers in series, or by tripping one circuit-breaker before another, depending on the conditions to be safeguarded.

It is of interest to note that no protective relays operated incorrectly during the tests.

(5.2) System Faults Encountered During the Tests

During one day lightning was reported in south-east England. Testing continued for a while but a continuous check indicated that storms were moving into the area. The tests were immediately abandoned, protective gear was restored to service, and the system restored to normal. The last trip link had just been inserted for the main protection on the Cliff Quay–Yarmouth 132kV feeder when the line was struck by lightning and tripped correctly. Soon after this feeder had been restored to service, both the Cliff Quay–Sundon 132kV feeders tripped owing to lightning. All circuits, including the test generator, cleared correctly, and this was the only occasion when this generator was tripped from the system.

Testing was subsequently resumed, but the incidents clearly demonstrated to all concerned that the work was not being carried out in a laboratory and that the normal hazards of system operation must be catered for in planning system tests.

(6) GENERAL CONCLUSIONS

The programme succeeded because of the co-operation of all parties, and it was a real example of team work. The relatively gentle progress in the early stages was amply rewarded by the operating technique developed by the staff as the tests proceeded.

In tests of this nature, it is essential to draw up a programme and adhere to it, as any modifications, however slight, may delay the proceedings. Adequate communications, independent of the operational system, are very valuable, and a closed-circuit television installation can be most useful in giving the control-room staff a picture of conditions on the test turbine.

Some operational risks are unavoidable, but they must all be carefully considered and the possibility of fault conditions must not be ignored.

The tests demonstrated that the normal generator control-

panel instruments are inadequate and can give misleading information during unstable conditions. They also showed the usefulness of rotor-angle indication or recording in a large generating station where instability is likely to occur. These indications are invaluable in informing the operating engineer which generator has an oscillating rotor and in telling him if corrective action is necessary and, if so, whether the action is having the desired effect.

The tests showed that operational staff at large generating stations could benefit from training to meet and rectify generator instability conditions. This could be achieved in some measure by organizing small-scale demonstrations at stations where instability may arise.

(7) ACKNOWLEDGMENTS

The authors wish to acknowledge the support given by Mr. W. N. C. Clinch, the Controller of the Eastern Division, Central Electricity Authority, and the co-operation of all their colleagues in the Central Electricity Authority and in the Electrical Research Association who contributed to the test programme.

Acknowledgment is also due to Marconi's Wireless Telegraph Co., Ltd., who provided the industrial television equipment.

DISCUSSION ON THE ABOVE TWO PAPERS BEFORE THE INSTITUTION, 6TH MARCH, 1958

Mr. L. Gosland: These two valuable papers represent the attainment of an object which has been pursued steadily since 1948, when Mr. F. C. Winfield asked that the E.R.A. should examine the parameters and assumptions used in system studies. The tests described were preceded by examination and definition* of all constants used in system studies, by listing† those essential or desirable for the various types of study and by an inquiry into the accuracy with which the constants required could be evaluated from design or test data.

This tedious preparatory work, which drew help from most organizations concerned with the problems under discussion, made possible the establishment of precise questions which tests could be designed to answer.

The questions having been set, the organization described in the paper by Dr. Last and Messrs. Mills and Norris came into play. The problem seems rather simple; i.e. to find a suitable piece of system, isolate a machine and put it on a test busbar, connect it to a system busbar and conduct a few tests, but far-reaching precautions had to be taken. These proved both necessary and sufficient, and the paper provides admirable guidance for future occasions of the kind. The authors have not mentioned the provision required for many visiting engineers on the site, some engaged on investigations divorced from the main theme for which the occasion provided a unique opportunity.

The authors conclude that indication of the rotor angle of machines in control rooms is desirable. Has any further consideration been given to this, and is it practicable, or is equipment available? They also conclude that operating staff should be trained in the correction of instability and suggest small-scale demonstrations. Do they propose demonstrations on machines or on simulators, and has there been any action?

The outstanding conclusion of the paper by Dr. Busemann and Mr. Casson is that which all concerned hoped for, namely that the simple assumptions and simplified constants normally used in analysis for transient stability are entirely adequate. Further discussion of the detail would be helpful, and particularly of the statement that both flux decay and voltage-regulator action may be safely neglected. The two effects compensate, since neglect of flux decay is equivalent to the assumption that a voltage regulator maintains flux. The detailed evidence in support of this view in Table 7 relates to a test with voltage regulator already in action at the end of a fault of fairly short duration. Longer fault durations with greater flux decay, slower voltage regulator and slower governor might make it necessary to watch the position.

The important adverse effect of flux decay draws attention to the advantage of maintaining the flux, and thus all synchronizing forces from the start of a fault. In the extreme, there could be a computer fed with information on all relevant system quantities, instructing a powerful excitation system to provide optimum field throughout. Russian experiments in this direction have been described,* and it would be interesting to learn of the recent British developments and perhaps to discuss the advantages of improving stability by such means rather than by reduction of reactance.

The steady-state tests, which are a side issue in the present series, are probably better discussed in the context of results of a more elaborate series on this aspect recently completed by the Central Electricity Generating Board. One point indicating uncertainty should be mentioned, since a paper of the present character should be decisive. There is some indecision as between Sections 2.2 and 6.1 on whether a saturated or unsaturated value of synchronous reactance should be used to calculate the limit of leading reactive power. For the machine used, even at rated voltage, there is no great difference between the two values. At the test voltage (about 90% machine terminal voltage at critical times) the difference will be less. It is thus unlikely that saturation could have an important effect. The discrepancy between calculation and observation may be due to an effect of saliency.

The work resulting in these papers has achieved its object as concerns turbo-alternators, and we can now speak with great authority on matters of stability of networks connecting thermal stations. The immediately profitable application of the results is in the export field. It is pertinent to consider whether there is not need for tests of a similar character in relation to hydro-electric machines, where the factors enter into the problem in different relative orders of magnitude.

Mr. F. J. Lane: Full-scale system tests are often asked for, but the amount of preparatory work involved is rarely appreciated. The paper by Dr. Last and Messrs. Mills and Norris is important and valuable because it sets out clearly the many and complicated arrangements which have to be made. In Section 2 they could have added three other major aspects:

(i) To inform and obtain the co-operation of staff at all levels, by giving to all a reasonable understanding of the intention of the programme, of the special points requiring attention, and of the responsibility of each individual for observation and security.

(ii) To minimize the costs of the exercise, which can be appreciable having regard to staff time, test rigs, spare plant and special duties on the day, quite apart from the capital value of the assets tested, which may run into some millions of pounds sterling.

(iii) To control the number and activity of those wishing to participate, in order to ensure quiet and objective co-operation, to satisfy those with special interests, and yet to prevent enthusiastic witnesses from unwittingly bringing themselves or their colleagues into danger.

In Section 7 of the paper by Dr. Busemann and Mr. Casson the authors should have answered quite firmly their own question as to 'how accurately (for the Cliff Quay conditions) the network-analyser studies represent actual system conditions', and avoided such vague phrases as 'in principle', 'reasonably accu-

* B.S. 2658: 1956. Guide to Terms used in A.C. Power System Studies.

† 'Data required for the Solution of Power System Problems' (E.R.A. Report Ref. V/T132).

* VENIKOV, V. A., and LITKENS, I. V.: 'On the Effect of Excitation Control on the Transmissible Power of Long Transmission Lines', *Elektrichestvo*, 1955, No. 11, p. 15.

rate' and 'not so material'. They could thus have given added significance to the tests and firmer guidance to future system analysts.

With reference to test No. 1.2, is it not possible that the different results from the *micro-réseau* analysis and the site test may be attributable to differences between the assumed and actual electrical quantities in the machine?

Monsieur M. Magnien (*France*): In France we are carrying out the commissioning tests for the first 380kV line between Génissiat and Paris, and so we can appreciate the amount of work represented by the organization, the tests themselves and their interpretation.

The *micro-réseau*, which is also called a dynamic reduced-scale model, is a 3-phase model and is operated at the normal frequency of the French system, i.e. 50c/s. It was designed between the years 1940 and 1949 by M. Roger Robert, under the supervision of M. François Cahen.

The elements of which it is constituted are as follows: the generating units, which are small real 3-phase generators, are driven by d.c. motors, representing the turbines. The generators have been studied so that their essential characteristics—saturation condition of the magnetic circuit, reduced reactances, time-constants of the circuits, as well as the characteristics of the exciters—may approximate as closely as possible to those in the actual machines.

The turbines are represented by d.c. motors with a separate excitation and with a variable-voltage supply, which makes it possible to adjust at will the speed/torque characteristic of the turbine which it is desired to represent.

The generator excitation is provided by individual exciters controlled by voltage regulators. The micro-turbine control is effected by means of electronic speed governors specially designed to represent the actual characteristics of steam and hydraulic turbine governors.

These small generators, which as a whole constitute the production centres, can be connected to a reduced-scale 3-phase system composed of resistors, reactors and capacitors, from which are supplied 3-phase sets of resistors and reactors representing the consumers' load.

The study of steady-state conditions on the *micro-réseau* demands, as on the actual system, the synchronization of the different sources. From these steady-state conditions we can study, by means of recordings, transient conditions due to the appearance of interference. This interference is provoked by a synchronous switch which acts on the system through a set of relays. This switch controls with precision the programme according to which the successive operations—application of the fault, fault duration, single-pole or 3-pole tripping of certain lines, etc.—must occur. The phenomena are recorded according to their nature and duration by means of cathode-ray or magnetic oscilloscopes.

In the paper by Dr. Busemann and Mr. Casson, we agree in ascribing the most important part of the differences between the results of the actual tests and those of the *micro-réseau* to the losses in the short-circuit, which are different for the actual machine and the micro-machine used.

With regard to the losses when short-circuiting the machine, it should be noted that this element has a great influence on the elimination times only when the fault is electrically quite close to the machine, and that this influence is greater as the power supplied before the short-circuit is smaller, since, short-circuit losses are practically independent of the initial load condition. This is emphasized in Table 5, from which it can be seen that the most important difference between the results of the *micro-réseau* study and those of the study on the actual system corresponds to Test No. 1.5, the 3-phase fault at the transformer terminals, the machine running at three-quarters of its rated capacity.

In order to make the studies on the *micro-réseau* of value in these particular cases it is necessary to try to reduce the losses of the micro-machines. This problem has been attracting our attention for a long time, and the Cliff Quay tests have induced us to pursue our efforts in this direction.

In Section 2.5 the authors indicate a law of the increase of losses in relation to the reduction in size of the machine. In fact, with micro-machines this law has not been exactly verified, owing to their special design. In particular, the micro-machines are very largely over-dimensioned with regard to the stator losses, so that at present the d.c. resistances of the stator windings are, in relative values, equivalent to those of the large machines. However, extra losses in the stator in the micro-machines are much higher than in the actual machines.

We are now trying to find constructional arrangements which will make it possible to reduce these losses to a value comparable to those of the large machines. Once a solution is found it will be very interesting to review some of the tests, such as Nos. 1.2 and 1.5, for which the parameters relating to the speed governor, voltage regulator, transformers and their related equipment are known, following the analysis of recordings obtained in the actual tests.

Mr. W. N. C. Clinch: I visited the Cliff Quay station, and, had I been one of the control engineers, I should have been very frightened to see the instruments behave as they did, and should have wondered what was going to happen next. I should like to know whether the manufacturers are, in fact, interested in seeing what their plant can do. Probably they are now very thankful, but were they, at the time, very appreciative of the fact that what had been designed and put into operation was able to carry out the obligations which these tests imposed on it? Furthermore, were the machines specially adapted for this occasion?

What lessons were learned from the tests? Can some indication be given of the way in which the order of procedure was defined? With regard to the switching of an unexcited rotor on to the system, what was the reaction at that time as a result of that sequence?

In Dr. Busemann and Mr. Casson's Fig. 4 it is of interest that the rupturing duty on the main field circuit-breaker, should it be called upon to open under pole-slipping conditions, is 170% of the figure at full load. Designers should note this, because field circuit-breaker failures are not unknown.

Mr. T. M. Whitelegg: With reference to the paper by Dr. Last and Messrs. Mills and Norris, the alarming performance of the generator switchboard instruments might not be so serious in temporary unstable conditions arising in practice, since some time would elapse before the control-room staff could decide exactly what had happened, and by that time the oscillation on the meters of generators which had retained stability might have died down.

In the paper by Dr. Busemann and Mr. Casson it is unfortunate that conclusions based on a theoretical assessment of the assumptions made in analyser studies are confused with conclusions based on the practical results achieved at Cliff Quay.

The overriding conclusion is that stability in the first swing was maintained at Cliff Quay to within the time limits determined by network-analyser studies. Probably the closest comparison is obtained in test No. 1.6, where the network analyser gave 0·36sec for the critical switching time and the test at 0·32sec at Cliff Quay produced stability. It is practically impossible to compare results by the extent of the swing, and therefore within the limits permitted by the number of tests available at Cliff Quay there seems no evidence to support the conclusion that the net-

work analyser using the usual assumptions is slightly on the unsafe side.

On the analyser the critical switching time is usually determined to within 0·025 sec, but even this is not always possible owing to the laborious step-by-step methods involved. The installation of automatic devices used in conjunction with network analysers, such as those recently described by Kaneff,* would enable critical switching times to be determined rapidly and accurately. For a given system where all the data were accurately known and a thorough transient-stability calculation was made giving a critical fault clearance time of 0·36 sec, what switching time would be selected by system operating engineers for everyday use?

If it could be shown on a particular system that stability would definitely be regained after pole slipping, I wonder whether this would be accepted as a feasible operating condition.

The tests at Cliff Quay have provided an all-too-rare opportunity to test network-analyser results in practice; the tests have shown that for this particular type of system, i.e. a generator supplying power over a long line to a very large system which behaves practically as an infinite busbar in comparison with the size of the machine tested, the analyser gives reliable results. It is hoped that, in the future, an opportunity will be afforded to test analyser calculations for other types of system.

Mr. R. E. Martin: From what the authors have stated, it is clear that rotor-angle instruments should be used. Can the authors state what types of display they would like to see? Would they prefer to see the rotor angle recorded continuously on a pen recorder with self-starting paper drive, or would they prefer a long-afterglow oscillograph tube? On the organization of large-scale tests generally, I certainly agree with the comments in the paper by Dr. Last and Messrs. Mills and Norris on the necessity for a really clear organization, and would underline the need for a clear plan. It is also necessary to have a properly defined line of responsibility, co-operation and safety, and I would also emphasize the need for good communication facilities and really reliable instrumentation. We have suffered from failures of both in the past. It is always a good plan to have a complete dress rehearsal of the instrumentation and to make arrangements to have everything ready and working a day before the tests are due to begin. Experience has emphasized these points and shown that there is a real need to mount all the instrumentation in a vehicle, so that it can be set up properly in the laboratory and transported to the job.

One difficulty with these tests is that we rarely have enough channels available in the oscillograph. I can remember the time when we used to be satisfied with three channels and for some years we have used six, and recently the number has been increased to twelve or more. We are now pleased to find that the instrument manufacturers are making 50-channel oscillographs with 12 in-wide paper. I would welcome the introduction of ultra-violet recording in place of photography. For many purposes this system, which produces an immediately visible record without processing, will be of great advantage to those who, like myself, have to organize tests in the field. The use of photographic techniques involving conventional processing is always inconvenient in the field owing to lack of water supplies and delay in awaiting each record before tests can proceed.

Perhaps the power engineer should adopt the magnetic data-recording techniques which are now used in the aircraft and guided-missile field for multi-channel recording.

Mr. D. R. Fenwick: The value of the automatic voltage regulator has been demonstrated during these tests, but the result given in Fig. 17 and discussed in Section 6.3 of the paper by Dr. Busemann and Mr. Casson seems likely to lead to the wrong conclusion.

The regulator concerned is of the normally inactive type having a dead band or zone of insensitivity of about $\pm 0{\cdot}5\%$. When such a zone of insensitivity exists in the regulating system, conditions inside this zone correspond to those of manual control, and it is not, in fact, possible to obtain truly stable operation at power angles which exceed about 90°. Any attempt to operate slightly beyond this point will result in a slow increase of angle which, in turn, causes a slow reduction in alternator terminal voltage until the fine raising contacts of the automatic voltage regulator operate to increase the excitation of the machine to reduce the angle and raise the terminal voltage. The fine lowering contacts now operate to reduce the excitation and increase the angle again. The phenomena are repeated continuously, the frequency of swing being about one cycle per minute.

If operation at even greater angles is attempted, the rate of decrease of voltage with increase of angle is such that the forcing contacts which are set at 5% below the normal value operate to raise the excitation rapidly. Under these conditions the oscillation of the rotor, which was relatively small and of long periodic time, increases in magnitude and frequency to a violent swinging with ultimate loss of synchronism, and I believe that Fig. 17 shows an early part of such a swing.

Steady operation at power angles in excess of 90° can only be obtained with a suitably designed continuously acting regulator, and it is regretted that comparative tests with such an equipment were not made at Ipswich.

It is of interest to consider the steady-state limits which would have been expected with the magnetic-amplifier/amplidyne

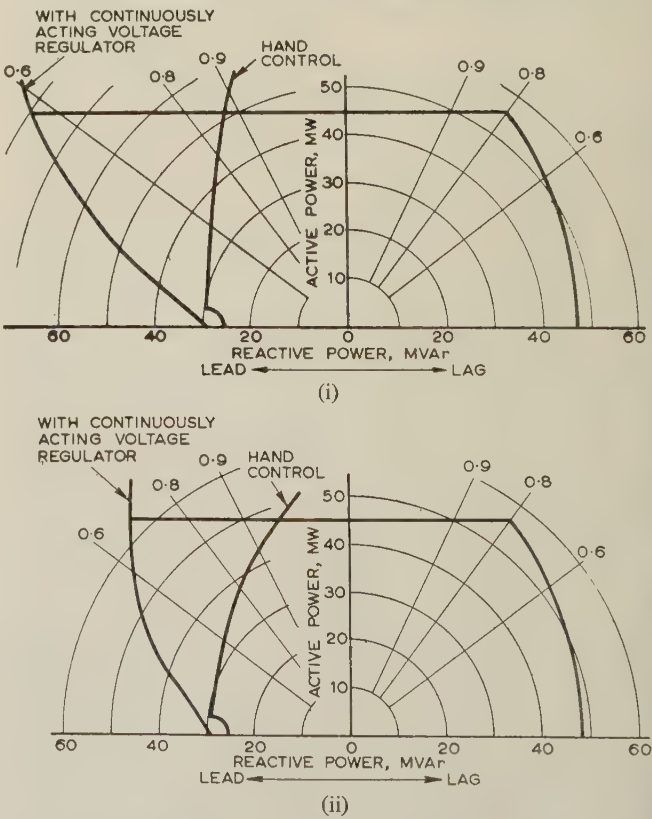

Fig. A.—Operating chart for 1–45 MW alternator at Cliff Quay.

(i) When connected to the system through a transformer.
(ii) When connected to the system through a transformer and the Sundon line.
Stability limits of alternator terminal loading. 11·8 kV terminal voltage.

* Kaneff, S.: 'Dynamic Operation of an A.C. Network Analyser', *Proceedings I.E.E.*, Paper No. 1847 S, October, 1955 (102 A, p. 597).

regulator, and accordingly an analogue-computer study has been made to determine these limits both with and without the Sundon line [Figs. A(i) and A(ii)]. With reference to Fig. A(i), it is of interest to note that the power angle of 130° in Fig. 17 corresponds to a power factor of approximately 0·6 (leading) at 30 MW, whilst in Fig. A(ii) this corresponds to a power factor of approximately 0·65 (leading).

Mr. B. Adkins: The process of simulation has become very important in relation to power-system work, owing, on the one hand, to the difficulty of carrying out direct experimentation of the kind recorded in these papers and on the other to the complication of a purely theoretical approach to the subject. The process can be regarded either as a means of representing the system in order to find out how it works (the *how* method) or as a means of making the calculations required by a theoretical study (the *why* method).

At the Imperial College of Science and Technology we have an installation of micro-machines which provide an extremely good simulation of the machines and auxiliary apparatus. The equipment was provided by the Central Electricity Generating Board with the collaboration of Électricité de France.

I gather that the French micro-machine equipment (as well as the more recent Russian installation) has been primarily regarded as a means of setting up the system and making a test; i.e. they use the *how* method. On the other hand, we are trying to use the *why* method. Our plan is to start by calculating the theoretical performance and then to check the result by experiment.

So far, we have dealt only with relatively simple conditions. The subjects investigated include forced and free oscillations in synchronous machines, rapidly oscillating short-circuit torques which may produce dangerous stresses, slowly varying torques after a short-circuit or a fault which cause swinging of the rotor, the asynchronous operation of synchronous machines, the conditions of resynchronization, and finally the effect of voltage regulators of various types on the stability of the machine.

Mr. J. S. Vosper: The investigation of system stability at Cliff Quay provided an excellent opportunity for studying switching transients. These are of considerable interest, and I should like to indicate briefly the main features of these measurements.

The voltages to earth at each terminal of the circuit-breaker clearing the short-circuits were recorded by cathode-ray oscillographs. Two system arrangements are of interest:

(i) The clearance of a phase-to-phase fault fed by the test generator in parallel with the main system.

(ii) The clearance of a 3-phase fault fed by the test generator alone.

A comparison between a test record and a record obtained on the E.R.A. network analyser for the first arrangement is shown in Fig. B. Allowing for the width of the trace in some portions of the test record, indicated by the multiple dotted lines, the agreement is seen to be quite good.

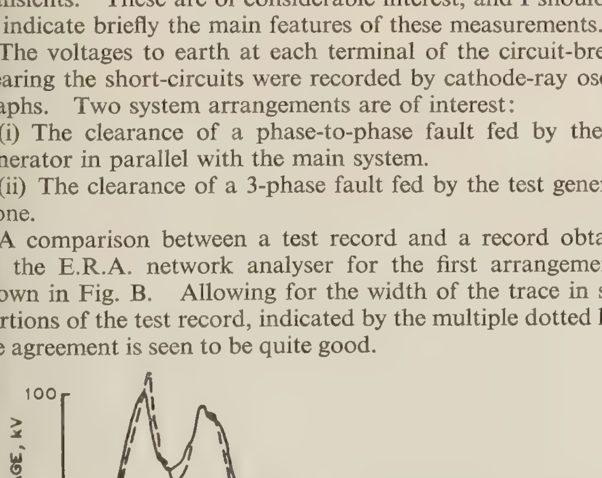

Fig. B.—Comparison between network analyser and test records.
——— Analyser record.
– – – – Test record.

The frequency of the recorded restriking voltage transient for the second case is in close agreement with that calculated from normal parameters for the local plant involved. The damping of the oscillation is in reasonable agreement with that measured on other, similar, plant. At the greatly reduced excitation due to the abnormally long fault duration in this test an interesting and important characteristic of the generator is apparent: the mean line of the oscillation approaches the extrapolated 50 c/s recovery voltage exponentially during the first quarter cycle.

A more detailed account of these results will be published as soon as possible.

Dr. P. D. Aylett: If we consider this question of increased rotor losses on the isolated generator, i.e. the subject of test No. 2, we see that the increased losses arise, as would be expected from the theory, because the very large subtransient currents produce losses not only in the stator and transformer resistance but also in the rotor resistance. This rotor resistance is more or less measured by the negative-phase-sequence resistance of the machine, and a simple calculation shows that, at the instant of the fault, something like full-load torque has to be put into the losses, and so we would expect that initially the machine would not accelerate at all. However, the position soon changes with the rapid decay of the subtransient currents (and torques).

The authors not only give us a very good comparison with the techniques which have been adopted for years in solving power-system problems, but signpost the way to the future. There is no doubt that, for turbo-generators on the British system, almost any generator will resynchronize following a fault. This tends to reduce the importance of the rapid clearance of faults. For simple systems it appears, however long the time to fault clearance, that the generator will resynchronize, and this view is confirmed by theory. This, if it is true for complex systems and is confirmed by further investigations, might fundamentally influence methods of design of power systems.

In Section 5.3.2 of the paper by Dr. Busemann and Mr. Casson, the damping of the machine is shown in the asynchronous tests to be very high. The machine had a negligible slip at 30 MW, and the damping torque coefficient instead of being 8 or 9 was about 50–100. This value is effective only up to about $\frac{1}{2}$% slip. The damping torque coefficient is very large when the slip is small, so that if the governor brings the machine into this region it will resynchronize; but the governor can have a negative effect, as the first two diagrams of Fig. 6 show. In this case the effect of the governor is such that most steam is admitted at the instant when the slip velocity is a maximum. In other words, the governor system produces negative damping, and it pumps power in when it should be taking it out.

We have tried to obtain some idea of how governors perform and their mathematics as control mechanisms, but the manufacturers do not seem to be able to tell us. We want to know a good deal more about what the governor does at the various slip frequencies which it is likely to encounter, particularly when the machine is out of step.

The paper is very important, because it fills a gap between theory and practice. Anyone who has studied this field finds many elegant equations and a great deal of theory but very little experimental evidence. It is just the sort of evidence given in this paper which will be of value to us. These tests have also encouraged the Central Electricity Generating Board to carry out a number of other tests. We find that the turbo-generator is a very good machine indeed. Provided that the three phases are connected we can make it slip, and switch the field on or off and it will nearly always resynchronize. Thus, although the conditions may look dangerous to the operator watching his violently oscillating instruments, they are much safer than they look.

It would be a good plan to carry out tests to show the control

staff that they can easily, by taking simple action, restore a machine to synchronism. This need not be done with a simulator; it can be done in the power stations. We have done it in power stations at various times, and operators soon gain confidence when they find that simply by increasing the field current they can bring a machine back into step.

Mr. E. B. Powell: During these tests I was recording the rotor angles on a ciné film from stroboscopic observations.

From an analysis of the film in test No. 2 it was seen that the initial braking constant due to the heavy losses with a 3-phase short-circuit would exceed the acceleration constant due to full-load steam input, and so the rotor would initially slow down slightly before accelerating. This initial braking effect is not always appreciated, but it can be seen from curve (i) in Fig. C,

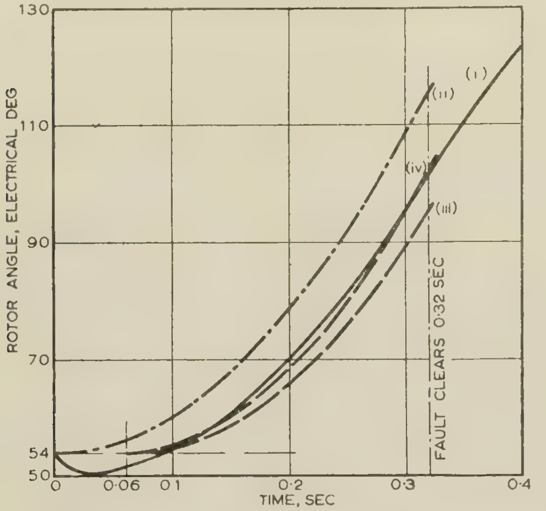

Fig. C.—Three-phase fault of 0·32 sec duration. The machine load is 45 MW, 10 MVAr (lagging).

(i) Stroboscopic record.
(ii) As calculated by acceleration equation with $H = 5 \cdot 85$ kW-sec/kVA.
(iii) As for (ii) but with a time delay of 0·06 sec before acceleration.
(iv) As for (iii) but with $H = 4 \cdot 97$ kW-sec/kVA.

obtained from the ciné film in test No. 1.6. It is impossible to decide the amount of the actual back swing, since the reference voltage will be distorted when the fault is applied. In my opinion, however, the true back swing would be about $\frac{1}{2}°$ instead of the 3° shown by the film.

Curve (ii) shows the calculated angle advance with $H = 5 \cdot 85$ kW-sec/kVA and ignoring resistive losses. It is known that there was no governor action during this period.

The equal-area theory shows that, if the acceleration followed curve (ii), the system was stable up to a fault-clearing time of 0·35 sec. However, it gives an angular advance of 15° too much during the fault. This confirms the view that the acceleration of the rotor must have been delayed due to the braking effect.

Curve (iii) shows the calculated swing curve, assuming the acceleration is delayed for 3 cycles, when $H = 5 \cdot 85$ kW-sec/kVA.

Curve (iv) is the same as curve (iii) but with $H = 4 \cdot 97$ kW-sec/kVA. This value of H was obtained from an analysis of the film showing the rotor acceleration during the first 20 cycles when the machine was switched out with a load of 4·0 MW. I should be interested to know the method of testing adopted by the manufacturers which gave $H = 5 \cdot 85$ kW-sec/kVA.

In order to bring curve (iii) into line with curve (iv), the delay in acceleration would be 2 cycles.

In view of the findings from these tests, I would stress that an expensive network analyser should never be used on problems that can be solved in half an hour by a simple approximation, such as that shown by curve (ii), unless this simplified approach suggests that the system is unstable.

Mr. J. G. Miles (*communicated*): The paper by Dr. Busemann and Mr. Casson brings into prominence the significance of the governor and regulator control loops in determining system behaviour. The results of the static stability tests clearly indicate the importance of the voltage regulator in extending the machine operating limits in the under-excited region, as previously described by Mr. Fenwick.

The ability to utilize this additional machine capacity is highly desirable to meet the large capacitive demands encountered under light-load conditions on present-day interconnected networks.

Alternatively the use of suitable voltage regulators may enable machines of lower short-circuit ratio to be used to meet given loading conditions.

In order to calculate the improvement in performance to be expected from the use of a given regulating system, the most suitable means appears to be an electronic analogue computer of the differential analyser type.

By the use of such equipment, and employing linearized equations, the response of the system to small disturbances under given operating conditions can easily be determined, and by taking a series of operating points, the dynamic stability limit curve can be built up. Computers of this type are also of value for detailed studies of transient stability, including governor and regulator effects, particularly when the second and subsequent swings are of interest.

With the increased emphasis now being attached to the performance of system control loops, as shown in the paper, the use of computer equipment of this type can evidently be a valuable aid to co-ordinated system planning.

Mr. D. B. Welbourn (*communicated*): Section 5.3.4 of the paper by Dr. Busemann and Mr. Casson is tantalizing in its brevity. Can the authors provide further details of the design of the governor and its operation? Inspection of the Figures suggests that, in fact, it is unnecessarily sluggish in operation, and that a better governor might appreciably improve the performance of the system. The performance of mechanical governors is subject to rational analysis* despite widely held views to the contrary.

It is surprising that the authors do not even comment on the need for governing from the electrical side of the system, instead of waiting for the speed to change in order to control the steam supply.

[The authors' replies to the above discussion will be found on page 373.]

NORTH MIDLAND CENTRE, AT LEEDS, 4TH FEBRUARY, 1958

Mr. W. J. A. Painter: In Section 3.1 of the paper by Dr. Last and Messrs. Mills and Norris, I expected to find reference to the auxiliary supply arrangements in the station. Can the authors say whether the 'test generator' auxiliary supplies were completely isolated from the auxiliary supplies for the remainder of the station?

With reference to Section 3.4, did the manufacturers take part in the pre-test discussions on precautions for safeguarding the plant, and did either the manufacturers or the C.E.A. examine the generator for signs of stress after the tests?

* Welbourn, D. B., Fuller, R. A., and Roberts, K.: *Proceedings of The Institution of Mechanical Engineers*, 1958, 72.

In Section 6 it is stated that the normal generator control instruments are inadequate and can give misleading information during unstable conditions. Can the authors state which instruments, in particular, were misleading or inadequate, which should be replaced and by what form of instrument, and what additional instruments they would recommend as standard fittings?

The very close agreement between the test results and network-analyser studies brought out in the paper by Dr. Busemann and Mr. Casson will give system design engineers increased confidence in the use of analysers and encourage them to proceed with further studies of system conditions. Nevertheless, the tests were made with one generator and one line—a relatively simple arrangement. Are the authors satisfied that similar close results would be obtained for the more complex systems which are normally studied on analysers?

In a few years' time the Grid system will, when operated most efficiently, at times of light load, consist of large blocks of generating plant (e.g. 550 MW sets at Thorpe Marsh or the 1 000 MW stations like High Marnham) in Yorkshire and East Midlands connected by long 275 kV lines to load centres in the South where there will be isolated smaller units of base-load nuclear generating plant. Will that system be inherently stable? Do the authors feel that the Cliff Quay tests have provided all the answers required to make studies of that system, or would they like to make more tests of a similar nature on a more complex network?

Apparently conflicting statements on generator-transformer tap positions are made in Sections 2.2 and 5.3.5. Can the authors comment on this and state whether the tap positions of the generator transformer during the tests were selected deliberately, and if so, can they give the reasons for their choice?

Further to the steady-state stability tests, do the authors consider it possible for steady-state instability to develop with lagging power factor at the generator terminals?

References are made in the paper to the marked effect of the automatic voltage regulators, which were of the type VS. Would there have been any marked improvement had the authors used the faster VSA type or the modern high-speed types now being installed at major power stations?

Finally, if the tests were repeated, what changes would be made in the test programme or in their organization?

Mr. D. R. Fenwick: There is no doubt that the tests carried out at Cliff Quay have yielded a wealth of information valuable to all concerned with power-system design.

It is disappointing that the *micro-réseau* analyser has not shown up too favourably, in that the excessive losses of the generator models have led to over-optimistic forecasts.

When conventional network analysers are employed the labour involved in accurately catering for the effect of automatic voltage regulators and governors is considerable and discourages detailed investigations. It would appear that, if further progress is to be made in system analysis, it will be necessary to employ either digital computers or alternatively large analogue computers to obtain more accurate system representation.

Test No. 1.5 is an example of the over-optimistic forecasting of the maximum fault clearance time by the *micro-réseau*, whilst reference to Fig. 5 indicates that, in this case, resynchronization was due to the combined action of the governor and the automatic voltage regulator. It is noted that the oscillation subsequent to pull-in was not damping out, and I suggest that this is due to a combination of the rising prime-mover input together with the falling rotor current as it approaches its final steady-state value under the control of the automatic regulator.

Mr. H. C. Ogden: The paper by Dr. Last and Messrs. Mills and Norris shows the immense amount of work which has to be put into the organization of tests of this nature, and also the care which was taken to maintain an uninterrupted supply to consumers.

Do the authors consider that the provision of rotor-angle indications as a standard in large power stations would be justified in the light of past experience and future expectations?

The paper by Dr. Busemann and Mr. Casson refers to ample time being available for corrective action to be taken by hand. I would stress that what is ample time to a man standing by the control panel waiting for an expected event to take place is far from ample under day-to-day operating conditions, when the need for action arises suddenly and unexpectedly and when the operator may be following a completely different train of thought and also may be some distance from the control point.

With regard to steady-state stability, in the Yorkshire Division we carried out some experiments last year at Ferrybridge on the effect on the reactive output of a machine of varying the busbar voltage. The results showed that saturation played a significant part in modifying the voltage/reactive-power characteristics, and they supported the suggestion in the paper that the reciprocal of the short-circuit ratio should be used in the calculations, rather than the unsaturated synchronous reactance.

Finally, I understand that in Russia large machines are put on load without synchronizing. Would the authors give their opinion on the possibility of this practice being adopted in this country?

Mr. P. Richardson: With reference to Section 1 of the paper by Dr. Busemann and Mr. Casson, an increase in short-circuit ratio which necessitates a larger air-gap merely results in an increase in frame size.

In Section 2.3, reference is made to the rotor slipping. Surely, at the time considered, the rotor is still in synchronism with the system and the rotor centre-line is merely advanced.

With reference to Section 6.1, I still consider that stability charts should be prepared on a basis of synchronous reactance, so that the known margin makes some allowance for instrument errors, etc.

The last two sentences in the penultimate paragraph of Section 6.4 appear to be contradictory. In the first of them, it is stated that with a low slip the rotor current corresponding to the torque must have found a low-resistance path in the damper winding and the steel. The slip conditions after transient faults must be very similar to those mentioned in the foregoing sentence, but the authors state that this low-resistance path is not available after transient faults because the penetration of alternating current in the steel depends on frequency. When excitation is removed from a synchronous generator, the slip is appreciably less with the field circuit closed than with it open, thus demonstrating that a large proportion of the machine flux links with the rotor winding.

Under working conditions, the flux density in the rotor may result in a permeability of about 30, i.e. less than the 1 000 assumed, and the depth of flux penetration may be 5 or 6 times greater than the figure given in Table 10. The flux therefore penetrates below the rotor slots when considering asynchronous operation. If the flux were confined to a thin layer around the surface of the rotor, the flux density would be extremely high, and the effective permeability may well be down in the region of single figures. I suggest that Table 10 is most misleading.

Mr. T. Wilson: In the past it has been possible to allow one's thinking about stability to be conditioned by the simplifying assumptions made in network-analyser studies. It therefore comes as a surprise to find that, in the tests in which instability occurred, resynchronization was effected so readily. It would be interesting to know whether this behaviour is likely to be typical. On the basis of Table 10 in the paper by Dr. Busemann

and Mr. Casson, the authors rule out the possibility of damping by rotor eddy-currents being effective after a transient fault. Is there any other evidence to support this conclusion?

Mr. W. E. Park: Fig. 18 and Section 6.4 of the paper by Dr. Busemann and Mr. Casson refer to asynchronous operation of the test generator with open field circuit. I should like to have more information on the operating conditions. It may not be sufficiently realized that when a synchronous generator has its excitation gradually reduced it eventually falls out of step and pole slipping occurs accompanied by surging. When the excitation is suddenly removed the alternator falls into the condition of an asynchronous or induction generator. This usually happens smoothly and the machine continues to carry its power output while operating at a leading power factor, i.e. it draws its magnetization from the system. There is some reduction in terminal voltage, but apart from the trouble of rotor heating by currents in the rotor iron, it may be capable of continuing in this condition for a length of time which must be decided by those on the spot. The latter point is mentioned at the end of Section 6.4.

The test results indicate that the governor of the machine was apparently in very good order, and this is supported by the film showing the governor in action. One feels that not every governor would show this degree of control, and that therefore the expectation of performance should be accepted in general with some caution as representing the optimum performance.

[The authors' replies to the above discussion will be found on the next page.]

NORTH-EASTERN CENTRE, AT NEWCASTLE UPON TYNE, 10TH MARCH, 1958

Mr. G. W. B. Mitchell: We seem to have been lucky with network-analyser calculations in the past, as various factors which have commonly been ignored tend to cancel out. As indicated in Section 3 of the paper by Dr. Busemann and Mr. Casson, some of these factors tend to worsen transient stability, e.g. flux decay, and others to improve it, e.g. the action of voltage regulators, damping and additional losses. Governor action can, however, be ignored during the first swing.

The authors mention the very interesting point, which has not been generally appreciated in the past, that under transient conditions, heavy initial losses in the alternator actually cause deceleration for a brief period. Since the Cliff Quay tests, this factor has been verified on miniature machines in a laboratory, and the phenomenon agrees with Concordia's theory that heavy initial losses will occur in the field and armature circuits.

It was understood after the tests that the *micro-réseau* results would be carefully checked with a view to determining why there were certain significant differences between these and the test results, and the authors mention that the *micro-réseau* studies did not properly represent losses, damping and governor action. Could they amplify this statement and also state whether this represents the whole story. If not, what other factors were concerned?

The position on steam governors is not as satisfactory as that for regulators, and I have always found it difficult to obtain reliable information about governor performance from the manufacturers.

The ideal governor would instantly adjust itself at all times to maintain a true balance between steam input and electrical output plus losses. This could obviously be best achieved electrically, and I suggest that the time is ripe for a concentrated attack on the problem. As indicated in Section 5.3.4, there will always remain some time lag, owing to the need to operate steam valves and the effects of entrained steam, but the overall response could, nevertheless, be considerably improved.

In making network-analyser studies it should be possible, with up-to-date knowledge, to assume a figure for the reduction in voltage behind transient reactance, and it would seem that such an assumption would alone almost compensate for the fact that ordinary analyser studies tend to be optimistic.

I have thought for many years that operators and machine designers have been unduly apprehensive about leading-power-factor and asynchronous operation and pole slipping. Pole slipping is certainly alarming to operators who are not used to it, as the panel instruments 'go quite mad'. However, the tests at Cliff Quay, and the subsequent tests which have been carried out in the north-east, should give everyone a great deal more confidence.

Have the authors arrived at any general conclusions on the behaviour of salient-pole alternators and hydro-electric sets under similar circumstances?

Mr. F. H. Birch: Although stability has seldom been a problem in this country, the incentive to reduce the capital cost of generating plant, coupled with more complete knowledge of the performance of alternators in the under-excited region, is likely to result in a reduction in machine short-circuit ratio and the use of high-speed continuously-active voltage regulators to improve stability. With the advent of computers, the most economical operating solution will probably lie in the direction of keeping a closer watch on stability margins than hitherto, in order to enable smaller margins to be employed with safety.

In view of the important part played by the governor and the voltage regulator under abnormal system conditions, it would now seem opportune to develop a single control mechanism for the turbo-alternator which would take into account, not only speed and terminal voltage, but also power angle and possibly also power and reactive outputs. The anticipatory performance of such a mechanism would be improved by supplying it with rate of change of speed, voltage and rotor angle. Such a step clearly calls for replacement of the present oil servo governor by an electric mechanism.

With reference to Fig. 18 of the paper by Dr. Busemann and Mr. Casson, tests of asynchronous operation carried out more recently on a 60 MW machine have shown that at two-thirds full load the slip with a field discharge resistor connected was 70% of the slip with the resistor disconnected. The latter figure agrees closely with that shown in the Figure.

Mr. G. Lyon: These tests provide a most valuable comparison with theoretical analysis, and I am sure that the results will be consulted for many years. Other tests already made or in prospect have been mentioned and I hope that these will soon be completed and reported. Engineers concerned with oversea systems would particularly welcome test results covering salient-pole machines.

The paper by Dr. Last and Messrs. Mills and Norris deals with the organization of the tests, and I feel that the importance of that aspect must be emphasized. The value of adequate preparation was amply demonstrated during the tests, not only in terms of the test results, but also in avoiding risk of damage to the plant and significant disturbance to consumers. The number of faults permitted (eight) was naturally much less than would be desirable academically for obtaining precise stability limits, so that the choice of test conditions required considerable judgment. The disparity between some of the analyser and *micro-réseau* results led to the choice of switching times further from the real critical value than one might have wished, but the results indicate the relative accuracy to be expected from the two methods of calculation.

Were any of the network analyser and *micro-réseau* studies repeated using the actual operating conditions in order to obtain a closer check between calculation and test results, and if so, with what results?

What terminal voltage applies in Fig. 16, since both normal voltage and 95% voltage charts are mentioned? Furthermore, the limit curves apply to particular terminal voltages which should be stated explicitly.

Fig. 10 shows no more than 10% flux decay for normal fault durations of about 0·2sec, so that the corresponding errors would tend to be less than those shown in Table 7, which is derived from decays over times approaching one second (see Fig. 9).

The equal-area criterion is a useful basis for a comparison of results, but it is not itself rigorously correct so that the relative errors given are not true errors.

It would be useful to publish a plot from the film record made of the movement of the governor and steam valves.

Are the switching over-voltages recorded from the test circuit-breakers to be published elsewhere?

It appears that the results of the tests, so far as they are generally applicable, give a welcome reassurance that the conventional methods of system design using network analysers give acceptable accuracy for normal engineering purposes. In the past we have been reasonably confident that these methods were sound, but the direct comparison carries more conviction than any amount of calculated checking.

THE AUTHORS' REPLIES TO THE ABOVE DISCUSSIONS

Dr. F. Busemann and **Mr. W. Casson** (*in reply*): We are very grateful to all who have contributed to a most interesting discussion.

We agree with Mr. Gosland that the individual errors need not always cancel out, but the development trend is towards shorter rather than longer fault durations so that the conditions become more favourable.

Some of the questions raised by several speakers relate to steady-state stability conditions and to the use of continuous-acting voltage regulation. Some interesting tests on this aspect have recently been carried out on the north-east coast, the results of which are to be the subject of a paper, and they should give the information required.

As Messrs. Gosland and Mitchell point out, the conclusions of the present tests with respect to resynchronization cannot be directly applied to salient-pole generators driven by water turbines because of the different governor characteristics, and further tests on such machines are desirable.

Further tests on more complicated systems, as suggested by Messrs. Whitelegg, Painter and Lyon, are also desirable, but they are less urgent because, in our opinion, they are not likely to yield a great deal more information.

The questions of Messrs. Lane, Mitchell and Lyon concerning micro-machines have been answered by M. Magnien's valuable contribution, for which we are very grateful.

We regret with Messrs. Whitelegg and Lyon that, owing to the inclusion of *micro-réseau* values, most of the fault clearing times chosen for the field tests differed too much from the network-analyser values for a direct comparison. But it is hoped that the assessment of the individual errors in Table 7, together with the information of Fig. 10, will give network-analyser operators a basis for estimating the accuracy of their results, both with present techniques and with possible future developments which may reduce fault clearing times and improve voltage regulators and governors, e.g. by the means suggested by Messrs. Welbourn, Mitchell and Birch.

Mr. Whitelegg's question as to what practical value of fault-clearing time would be acceptable to the system operation engineers if the best network-analyser result is 0·36sec cannot be answered generally because it depends on so many factors affecting the individual case.

We thank Mr. Fenwick for his interesting study on the possibility of raising the steady-state stability limit to power angles greater than 90° by continuous-acting voltage regulators, which is far beyond the objective of this group of tests at Cliff Quay.

Thanks are due to Messrs. Adkins and Vosper for their contribution; Mr. Vosper has answered also one of Mr. Lyon's questions.

Messrs. Aylett, Richardson and Wilson's questions concerned the contrast of the relatively small damping of the after-fault oscillations as apparent from the ratio of successive amplitudes with the high torque/slip ratio found from the tests with asynchronous operation. The contribution of the negative damping of the governor is much too small to explain this large difference, so that we had to look for another explanation. The greatest difference between the two cases seems to be the frequency of the eddy currents induced in the rotor by the relative velocity (slip) of the rotor with respect to the stator flux which is stated in Table 10, col. 1. The effect of frequency on the depth of penetration of these eddy currents is illustrated by the figures in col. 3. These have been obtained with the permeability shown, which was chosen only to illustrate the point. Other permeabilities as suggested by Mr. Richardson would give other depths of penetration, but the relative ratio of the values for 1 c/s during after-fault oscillations to those for one cycle in 6–36sec during asynchronous operation would remain the same.

We thank Mr. Powell for pointing out that his records show a small initial swing-back due to braking before acceleration during fault. Comparison with the *micro-réseau* results, where the braking effect was much greater, shows that the influence on the stability is relatively small at full scale. One reason why Fig. D (ii) differs from the actual result is probably neglect of the ohmic losses in the generator and transformer and of the movement of the system busbar voltage. The question of how H was measured by the manufacturers is outside the scope of the paper. We wholeheartedly agree with Mr. Powell that costly analyser studies should not be made in cases where results can be obtained in half an hour by simple approximations.

We thank Mr. Miles for his interesting note on the use of differential analysers for the study of voltage regulator and governor effects, and of transient stability conditions with consideration of the second and third swing.

The taps used in the field tests were decided by the station operators in accordance with normal practice at Cliff Quay. Section 2.2 refers to steady-state stability conditions, and the note on taps was meant as a guide to operators of generators without voltage regulators. Section 5.3.5 deals with transient stability and concerns the difference in the fault-clearing time limit that would occur if the transformer ratio used in practice differed from that assumed on the network analyser.

Steady-state instability with lagging power factor at the generator terminal is possible when the generator feeds a distant 'infinite busbar' over a high reactance.

Mr. Painter's interesting question whether there would be any changes if the tests were repeated would probably be answered differently by everyone concerned with these tests. Taking the test programme as a whole, there would probably not be much change, since tests on this scale are always a compromise between

many different interests. But many things have been learned about details which would enable improvements to be made on future occasions.

We agree with Mr. Ogden that what appears to be ample time in a staged test is not available in service conditions when instability occurs unexpectedly. It is gratifying to note that the steady-state stability-limit results found at Ferrybridge differ from theoretical calculation in the same way as those found at Cliff Quay.

Mr. Ogden asked our opinion about the acceptability of putting machines on the system without synchronization as the normal procedure. Our reply is that where the short-circuit level at the point of connection to the system is high the effect of exciting the machine field will only result in a small drop in voltage. Therefore this operation could be accepted as a normal procedure. Where the short-circuit level is relatively low the voltage drop might cause some difficulties, and the operation is not acceptable as a normal procedure.

Mr. Richardson's formulation confirms the interesting fact that even such an invisible thing as a larger air-gap costs money. We agree with his suggestion to go on using performance charts as calculated at present with the unsaturated value of synchronous reactance, and so to enjoy the benefit of a sound margin of safety.

The asynchronous operation of the test generator was as smooth as in the second case described by Mr. Park. In reply to Mr. Lyon, the terminal voltage for Fig. 16 is 95%. The conditions of Table 7 correspond to test 1.6 in Fig. 9 with a fault duration of 0·32 sec.

Dr. F. H. Last and **Messrs. E. Mills** and **N. D. Norris** (*in reply*): For brevity we shall reply to the discussion by subjects rather than deal with each speaker individually.

Co-operation of Staff and Order of Procedure.—The co-operation of staff at all levels is essential. Once the objectives were agreed, meetings were held between representatives of all departments concerned, particularly the operational staffs. Details were evolved at subsequent departmental meetings at all levels. This was the key to success. It will be appreciated that generalization for large-scale tests is impossible.

Cost.—The cost of system investigations can be minimized by careful planning and timing. The first requirement is to safeguard consumer supplies, and the running of spare generating plant is often essential. The plant, which has a high capital value, must be safeguarded. To economize in these two respects would be dangerous.

Manufacturers' Interest.—The manufacturers of the plant involved were consulted on the proposed programme, which was modified following their observations. Their co-operation in examining the plant afterwards was reassuring and was appreciated.

Subsidiary Investigations.—The response of manufacturers to invitations to carry out subsidiary investigations during fault and unstable conditions was disappointing. One protective-gear manufacturer gained valuable experience on distance protection. The organizers tried, unsuccessfully, to obtain a more modern type of voltage regulator. However, several different methods of rotor angle measurement were used by the C.E.A. and by the Imperial College of Science and Technology. The Post Office measured earth-system voltage rise, and the E.R.A. measured switchgear restriking voltages.

Auxiliary Supplies.—Electrical supplies which affected the performance of the machine were taken from a unit transformer connected to the stator terminals. All other electrical auxiliary supplies were taken from a station transformer.

Provision for and Control of Visiting Staff.—It was considered beyond the scope of the paper to detail the arrangements for accommodating, controlling and caring for about 100 engineers engaged on and witnessing the tests. The necessity of adhering to a prearranged programme is reaffirmed.

Recording during the Tests.—Recordings which did not require processing were very valuable, and the development of accurate records which are immediately available should be encouraged. This would reduce the time and cost of system tests. Power engineers are too conservative in recording methods, and the use of multi-channel aeronautical techniques should be encouraged.

Panel-Instrument Performance.—These instruments give an alarming picture of the conditions on an unstable machine. The field-current ammeter is the only useful instrument.

Rotor-Angle Instruments.—These instruments would be valuable at stations where instability is possible, particularly on large sets. Two indications are recommended—one to show the rotor angle on a dial, and the other a chart record, which, during disturbed conditions, will show the trend away from or towards stability. The control engineer will know whether corrective action is effective. An installation will be tried when minor difficulties of fouling the rotor reference point are overcome. An oscillograph display is not favoured, and indication of trend is considered the essential requirement.

Resynchronization.—The statement that almost any generator in Great Britain will resynchronize, regardless of fault duration, is not accepted. Experience does not confirm this. Systems often do not operate in the way they are designed; one or more trunk feeders may be out of service for maintenance reasons or for faults during bad weather conditions. A suggestion of attaching less importance to rapid fault clearance is deprecated.

Switching of Machine with Unexcited Rotor.—The proposed programme included switching an unexcited machine to the system with subsequent field restoration, but this was subsequently deleted by the organizing staff.

Machine Performance.—It has been incorrectly inferred that the machine was prepared for the tests and the results were therefore not representative. The machine had been in continuous operation for nearly 12 months prior to the tests. The satisfactory operation of the overspeed trip was the only prior check.

Staff Training.—The paper proposes staff training to correct generator instability. This should be done with running plant and not simulated. Correction is not just a matter of increasing field, as some speakers believe. Load reduction is often necessary as well. The operating engineer has to consider mechanical stresses, excessive rotor and stator temperature rises, fluctuating auxiliary supplies, and the reflection of large currents on to an already heavily-loaded transmission system. It is easy to theorize on the correction of instability, but an operator who theorizes with unstable plant may well court disaster.

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A FERROMETER FOR THE DETERMINATION OF THE A.C. MAGNETIZATION CURVE AND THE IRON LOSSES OF SMALL FERROMAGNETIC SHEET SAMPLES

By Professor H. BLOMBERG, D.Sc., and P. J. KARTTUNEN, M.Sc.

(*The paper was first received* 21*st February, and in revised form* 21*st October*, 1957. *It was published in January*, 1958, *and was read before the* MEASUREMENT AND CONTROL SECTION 11*th March*, 1958.)

SUMMARY

The paper describes the design and properties of a ferrometer for measuring small (about 30 mm × 200 mm) ferromagnetic sheet samples.

The test specimen is magnetized by an open-ended coil fed with a sinusoidal voltage, and the variation of magnetic flux with time remains practically sinusoidal up to 1·4 Wb/m². Feedback amplifiers and rectifiers are employed to deal with the electromotive forces proportional to the time derivatives of the field strength and flux density in the test specimen, which are induced in the pick-up coils, in such a way that the peak values of field strength and flux density can be read directly from moving-coil milliammeters and the iron losses from a wattmeter.

The measuring ranges of this instrument are:

Frequency: 50–250 c/s.
Field strength: 0·002–500 amp/cm when $f = 50$ c/s.
0·0004–100 amp/cm when $f = 250$ c/s.
Magnetic flux density: 0·0004–10 Wb/m² when $f = 50$ c/s.
0·00008–2 Wb/m² when $f = 250$ c/s.
Iron losses: 8×10^{-8}–5×10^{3} watts/kg when $f = 50$–250 c/s.
Cross-section of specimen: 4–40 mm².

The measuring ranges of field strength and magnetic flux density are dependent on frequency, and owing to the design of the instrument, the measuring ranges of the different quantities are partly also mutually dependent. In consequence, part of the above-mentioned measuring ranges have no practical significance (e.g. 10 Wb/m² when $f = 50$ c/s and 5 kW/kg).

LIST OF PRINCIPAL SYMBOLS

A = Loop gain at nominal frequency.
A_{Fe} = Area of cross-section of the test specimen.
B = Instantaneous magnetic flux density.
B_{max} = Peak magnetic flux density.
C = Capacitance.
H = Instantaneous magnetic field strength.
H_{max} = Peak field strength.
I = R.M.S. current.
L = Length.
M = Instantaneous magnetic potential.
N = Number of turns of a coil.
P = Power.
P_{Fe} = Iron losses.
R = Resistance.
$T = f^{-1}$.
V = R.M.S. voltage.
f = Frequency.
i = Instantaneous current.
k = Constant.
l = Length.
t = Time.
v = Instantaneous voltage.
Δv = Instantaneous value of the actuating signal to an amplifier.
Φ = Instantaneous magnetic flux in the test specimen.
Φ_W = Instantaneous magnetic flux in the wattmeter core.
α = Instrument reading.
ψ_n = Angular shift between the H_n and B_n waves.
μ_0 = Permeability of vacuum.
ρ = Density of the material.

Prof. Blomberg is Professor of Electrical Engineering, and Mr. Karttunen is in the Electrical Engineering Laboratory, of the State Institute for Technical Research, Finland. At the meeting the paper was read by Prof. F. Brailsford, Ph.D., B.Sc.(Eng.).

(1) INTRODUCTION

In 1945 the Electrical Engineering Laboratory of the State Institute for Technical Research (Finland) examined the problem of designing a laboratory ferrometer which would provide a simple method of measuring the magnetic properties of small ferromagnetic sheet samples. The basic physical relations chosen as a starting-point are well known and have frequently been applied in practice. For example, the instrument designed by Shenk[1] was probably the first direct-reading and absolutely calibrated device for the measurement of iron losses. Consequently, the paper does not present any fundamentally new method of measuring the magnetization curve and the iron losses, but only such applications and solutions as arose from the design work. The first measuring instrument was completed in 1950, and the second, improved, type in 1952. Although primarily designed to accommodate only small sheet samples, the magnetizing equipment and pick-up coils of the instrument may readily be modified to allow measurements on whole transformer and dynamo sheets.

The design of the measuring device emerged from the intention to prove the practical feasibility of the measuring principle presented in the following. Several design details had to be chosen rather arbitrarily, in order that the quantitative details could be approached and the difficulties encountered in the practical application of the principles could be found out. The main problem of the first prototype was the building of a satisfactory magnetizing device and the stabilization of the amplifiers. It was found that the magnetizing device containing an iron core was not satisfactory and that the stability of the amplifiers was poor. In the second model, which is described in the paper, the magnetizing device and the stability of the amplifiers were improved, and some details contributing to greater versatility were added. However, this model, too, is still in the prototype stage in several respects. It was impossible to investigate all detail aspects, and it is quite obvious that the measuring accuracy of the device can be improved and its design can be further simplified.

(2) PRINCIPLE OF OPERATION

A simplified diagram illustrating the operation principle of the ferrometer is given in Fig. 1.

The e.m.f. proportional to dB/dt, which is induced in the B-coil surrounding the homogeneously magnetized test specimen, is fed into an amplifier of very high input impedance, which produces an output current proportional to the input voltage

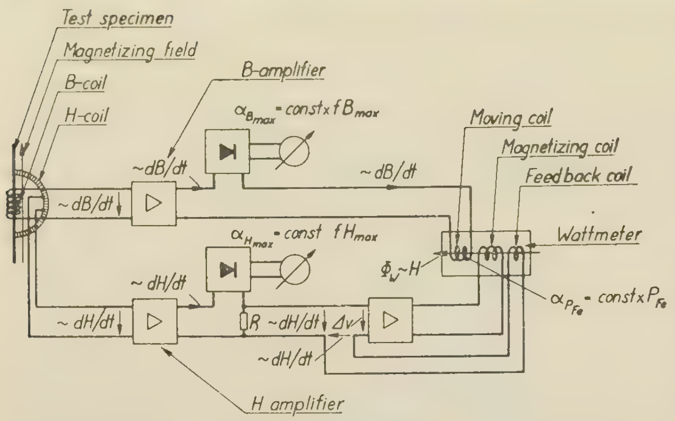


Fig. 1.—Schematic showing the principle of operation of the ferrometer.

at any time. The amplifier output current passes through the moving coil of a wattmeter, is rectified and fed to a moving-coil instrument, where it produces the deflection

$$\alpha_{Bmax} = \text{const.} \times fB_{max} \quad . \quad . \quad . \quad . \quad . \quad (1)$$

The e.m.f. proportional to dH/dt, which is induced in the Rogowski-type H-coil, is similarly amplified, and the rectified output current of the amplifier is measured with a moving-coil instrument, which shows the deflection

$$\alpha_{Hmax} = \text{const.} \times fH_{max} \quad . \quad . \quad . \quad . \quad . \quad (2)$$

A voltage proportional to the output current is also fed to an integrating amplifier, the output of which is the magnetic flux in the air-gap of the wattmeter; it is thus proportional to H. The wattmeter deflection will then be

$$\alpha_{PFe} = \text{const.} \times f\oint HdB = \text{const.} \times P_{Fe} \quad . \quad . \quad (3)$$

Eqns. (1)–(3) apply independently of waveform, provided that the e.m.f.'s generated in the pick-up coils retain the same sign during their half-cycles—a condition which is satisfied in this case. If, however, as is necessary in practice, only the iron losses corresponding to the fundamental frequency are required, the time variation of flux density must be sinusoidal. The iron losses corresponding to sinusoidally changing flux density will then be obtained directly in terms of the peak density.

It is also possible in the device described to superimpose upon the magnetizing field a d.c. component whose magnitude can be arbitrarily adjusted by calibration (see Section 3.3).

(3) CONSTRUCTION OF THE FERROMETER

In addition to the H_{max}, B_{max} and P_{Fe} measuring instruments, the device includes an adjustable 50 c/s source of power for the

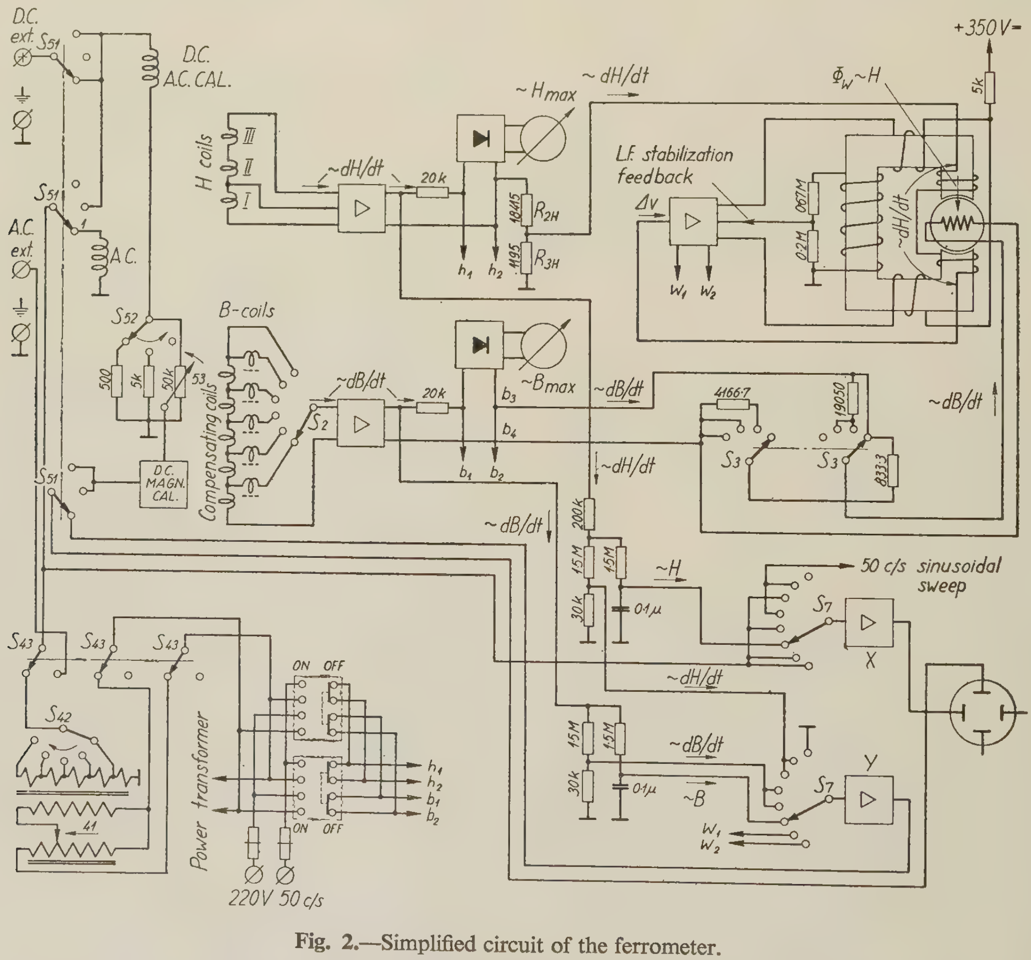


Fig. 2.—Simplified circuit of the ferrometer.

The following abbreviations are used:
μ, microfarads.
p, picofarads.
M, megohms.
k, kilohms.
Numerals alone, ohms.

a.c. magnetization, a calibrating unit for the d.c. component of the magnetic field, a small cathode-ray oscillograph and the necessary power packs. With the exception of the power supply, the connections of these units and the routing of the different quantities in the ferrometer are seen from the simplified circuit diagram, Fig. 2.

(3.1 Magnetization of the Test Specimen

The magnetizing device has two coils, and it is possible to magnetize the test specimen with a pure a.c. field or with a d.c. component of controllable magnitude superimposed upon the a.c. field. As can be seen from Fig. 3, the magnetic circuit

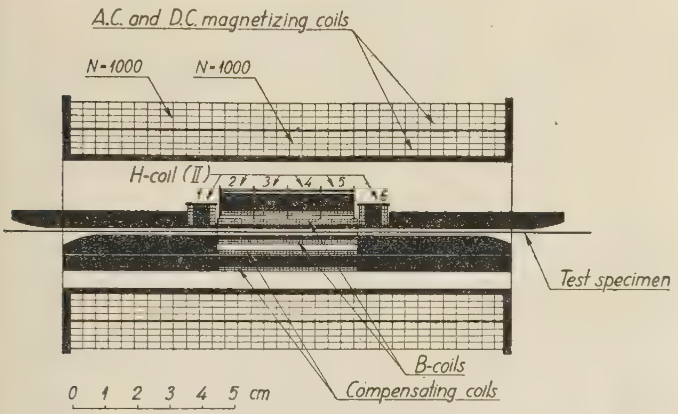


Fig. 3.—Design of the magnetizing and pick-up coils.
H-coils 2–5: $\simeq$ 6 600 turns total. Mean area per turn: $\simeq 0\cdot 47$ cm^2.
Coils 1 and 6: $\simeq$ 2 000 turns total.

B-coils:

B_{max} range, Wb/m^2	No. of turns
0·02	1 000
0·1	200
0·5	40
2	10
10	2

contains no iron, except for the test specimen of about 30 mm × 200 mm. This method of magnetization was chosen for the following reasons:

The section employed in the measurement is relatively homogeneous, as can be seen from the axial variation of flux density (Fig. 4). Although, for instance, the 1 Wb/m^2 curve shows a deviation of about 2·3% at the boundary of the measuring section from the mean value of density which the ferrometer

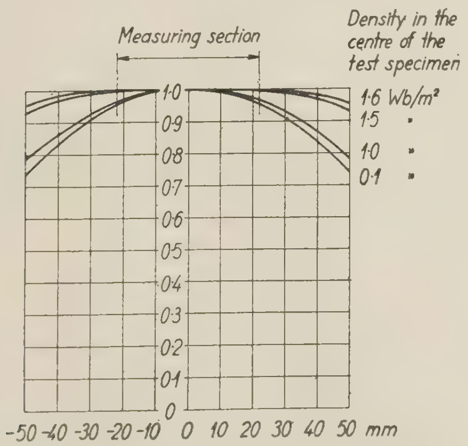


Fig. 4.—Relative distribution of the magnetic flux density in the test specimen (0·5 mm thick, 2·2% silicon).

measures, the error due to inhomogeneity at the corresponding point of the $B_{max} = f(H_{max})$ curve will not be more than about 0·5%.

When the magnetizing coil is fed with a sinusoidal voltage, the flux-density curve of the test specimen is relatively sinusoidal up to 1·4 Wb/m^2 (Fig. 5). Fig. 5A shows some dB/dt and dH/dt

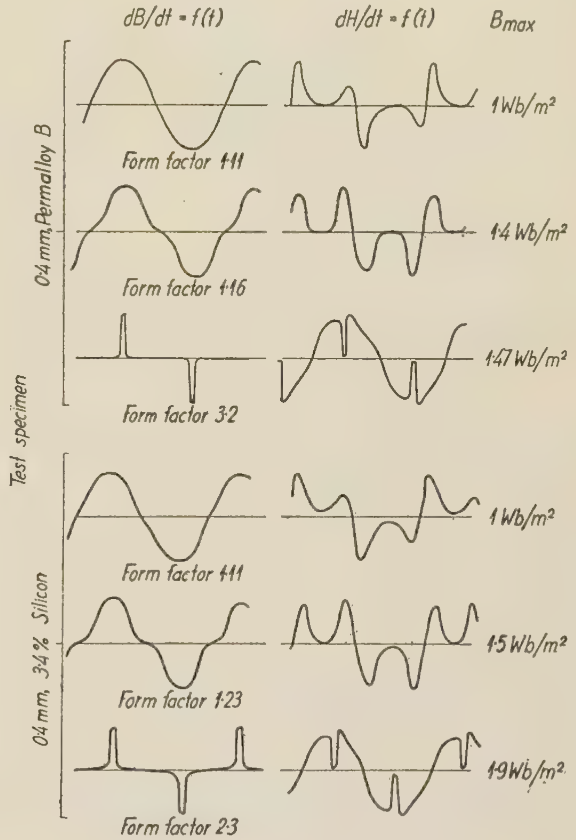


Fig. 5A.—dB/dt and dH/dt curves for test specimens of 0·4 mm Permalloy B and 0·4 mm 3·4% silicon; $f = 50$ c/s.

curves obtained by measurement, and Fig. 5B shows the amplitude relationships of the harmonics and fundamental wave of the B-curve.

The 50 c/s magnetizing current is supplied by the voltage source in the ferrometer (Fig. 2). The possibility of external feeding is provided for the achievement of any arbitrary a.c. magnetization at frequencies between 50 and 250 c/s and for d.c. magnetization superimposed upon the a.c. field.

(3.2) Pick-Up Coils

(3.2.1) H-Coils.

In theory, all H-coils should be as thin as possible and uniformly wound, and the coil ends should be pressed tightly against the test specimen. Since the manufacture of such coils is difficult in practice, each of the three identical H-coils placed parallel to one another is of the design shown in Fig. 3. The transverse parts 1 and 6 account for the change of the magnetic potential in the radial direction, the distance between the test specimen and these coils being about 1 mm. Coils 1 and 6 cannot be placed exactly at the ends of the axial coil, but must be displaced outwards by about 5 mm, giving rise to an error of about 0·5% in the measurement of field strength. The axial

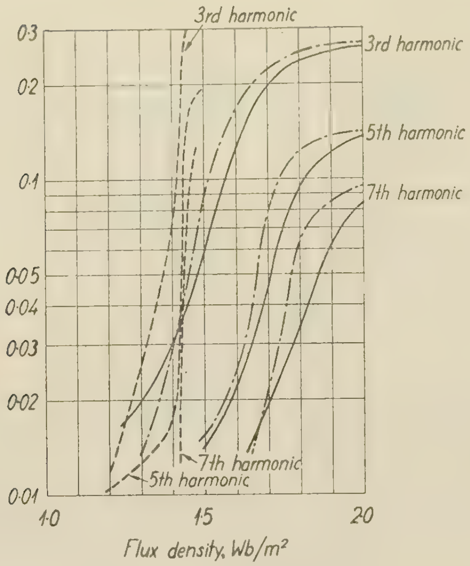


Fig. 5B.—Amplitude ratio of the harmonics and fundamental of the flux density, dependent on its peak value; $f = 50$ c/s.

——— Test specimen 0·5 mm, 2·2% silicon.
—·—· Test specimen 0·4 mm, 3·4% silicon.
- - - - Test specimen 0·4 mm, Permalloy B.

coil has further been divided into four separate sections in order to reduce its self-capacitance.

(3.2.2) *B*-Coils.

The flux in the air space between the *B*-coil and the test specimen has been compensated with the aid of a coil which, placed beside the pick-up coil proper (Figs. 3 and 6), cancels the entire flux present in the *B*-coil when there is no test specimen. Thus, strictly speaking, the instrument does not indicate B_{max}, but $B_{max} - \mu_0 H_{max}$; however, this is of very slight significance and has no effect upon the losses, since over-compensation occurs only with regard to the loss-free component. In order to provide several B_{max} measuring ranges, there are five *B*-coils, the compensating coil being tapped appropriately. The placing and the connections of the coils are seen from Figs. 2, 3, 10 and 11.

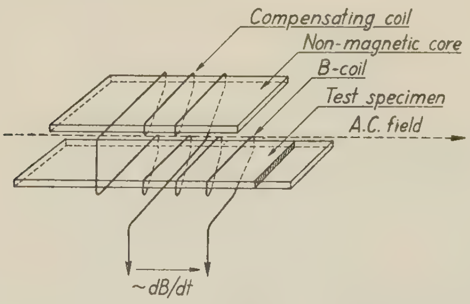
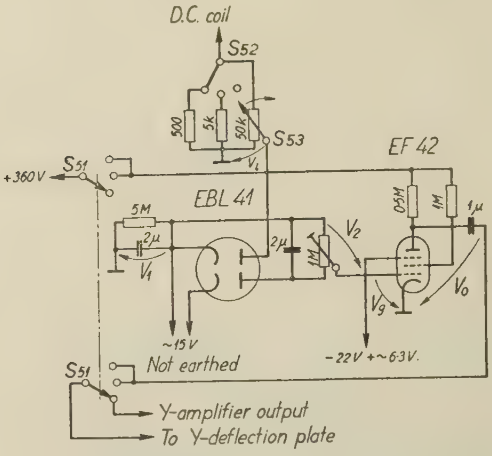


Fig. 6.—Compensated *B*-coil.

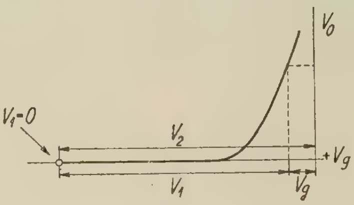
(3.3) Calibration of the D.C. Component of the Magnetic Field

The H_{max} instrument cannot be used to measure the d.c. component of the field, which requires separate calibration, as follows. First, to the d.c./a.c. calibration coil is fed an alternating current such that the measuring instrument with a.c. magnetization indicates an H_{max} or B_{max} value equivalent to the desired d.c. component. The coil is then fed with a direct current equal to the peak value of the alternating current applied. The d.c. magnetization will then—at least approximately—correspond to the H_{max} or B_{max} value indicated.

Fig. 7 shows the principle of the peak-current measuring instrument with both d.c. and a.c. operation. Switch 52 and voltage-divider 53 are used to adjust the measuring shunt connected in series with the magnetizing coil so that the input of the peak-measuring instrument, v_i, assumes a suitable value. The voltage v_i is rectified by means of a series-connected diode and an *RC* circuit with a time-constant of 10 sec. The resulting direct voltage, V_1, proportional to the peak value of the magnetizing current controls the EF42 pentode, which has an alternating voltage superimposed upon its suppressor-grid bias; its a.c. output, V_0, is thus dependent on the control-grid voltage, V_g [Fig. 7(*b*)]. The direct voltage V_2, which determines the ($V_1 = 0$) point of the EF42 valve, is sufficiently high that, at operation on the high dV_0/dV_g section of the characteristic, even relatively small changes in V_1 have a marked effect upon V_0 [Fig. 7(*b*)]. V_0 is indicated by a cathode-ray tube when switch 7 is in position 'D.C. CAL.' (full clockwise position). The position of the measuring



(*a*)



(*b*)

Fig. 7.—D.C. magnetization calibrating circuit.

The following abbreviations are used:
μ, microfarads.
p, picofarads.
M, megohms.
k, kilohms.
Numerals alone, ohms.

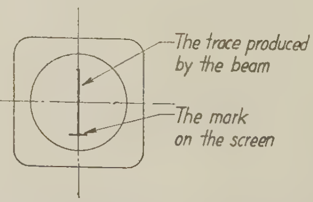


Fig. 8.—Use of the cathode-ray tube in calibrating the d.c. magnetization.

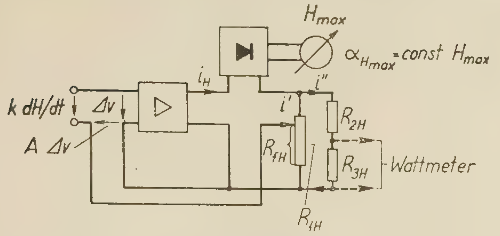


Fig. 9.—Schematic showing the principle of operation of the H_{max} measuring instrument.

Hence $i_H \propto dH/dt$ $\quad kdH/dt = (A + 1)\Delta v$; $A\Delta v = i'R_{fH}$, when $A \gg 1$

shunt (52, 53) is the same for both a.c. and d.c. measurement; it is adjusted for a.c. measurement so that the vertical line produced on the cathode-ray-tube extends to the mark made on the screen (Fig. 8).

(3.4) H_{max} Measuring Instrument

Fig. 9 shows the principle of operation of the negative-feedback H_{max} measuring amplifier, and Fig. 10 shows its circuit diagram.

The measuring range is changed potentiometrically by adjusting the feedback resistor R_{fH}. Simultaneously, the gain of the amplifier is adjusted so that the loop gain A (Fig. 9) remains approximately constant, i.e. at 400, with the exception of the most sensitive H_{max} measuring range, 0·1 amp/cm, where $A = 200$. On the basis of the resistances given in Fig. 10 and of the loop gain $A = 200$ it is easy to establish that the voltage gain from the grid of the first stage to the point between the 2 μF condenser and the 20 kilohm resistor in the output stage is about $1 \cdot 2 \times 10^6$ in the 0·1 amp/cm position. At full-scale instrument deflection, 0·5 mA, the reference input voltage will be about 2·6 mV.

For the ranges 0·1 and 0·5 amp/cm all H-coils are used, but for the other ranges only that in the centre of the test specimen is required.

The error of between −0·3 and −0·5% caused by the internal consumption of the rectifier has been allowed for by shifting the zero point of the instrument about 0·3% up-scale.

As a result of the high feedback factor, no other precision components are required in the circuit, apart from the feedback potentiometer R_{1H} and the voltage-divider R_{2H} and R_{3H}.

The output stage of the amplifier must be adequately dimensioned so that it can also provide the peaks of the dH/dt curve. In this circuit the amplifier is capable of delivering a current peak equivalent, at 50 c/s, to about seven times the full-scale deflection of the moving-coil instrument.

To eliminate high-frequency oscillations the amplifier has been stabilized with the branches R_1C_1, R_2C_2 and R_3C_3. Stability against low-frequency oscillations has been achieved by feeding-back of the triply integrated output voltage to the second amplifier stage. The l.f. stabilizing branch consists of the passive components R_4–R_9, C_4–C_6 and the pentode EF42.

The accuracy of the amplifier is $\pm 0 \cdot 25\%$ and that of the moving-coil instrument and rectifier together is $\pm 0 \cdot 75\%$; hence their combined accuracy will be $\pm 1\%$ of the full-scale deflection.

The instrument has been calibrated at 50 c/s. At other magnetizing frequencies, f_H, its reading must be multiplied by the correction factor $50/f_H$ [cf. eqn. (2)].

(3.5) B_{max} Measuring Instrument

The B_{max} measuring amplifier operates on the same principle as the H_{max} instrument, and its circuit is shown in Fig. 11. The measuring range is changed with switch 2, which connects the desired B-coil to the control grid of the first amplifier stage. Part of the voltage drop $i_B R_B$ proportional to the cross-section of the specimen is fed back, i.e. the voltage $k_1 A_{Fe} i_B R_B = i_B R_{fB}$, which, to a high degree of accuracy, is equal to the e.m.f. induced in the B-coil; this gives

$$k_1 A_{Fe} i_B R_B = k_2 A_{Fe} dB/dt$$

so that

$$i_B = k_3 dB/dt \quad . \quad . \quad . \quad . \quad . \quad . \quad (4)$$

The reading of the B_{max} instrument is thus directly proportional to the peak magnetic flux density, provided that the feedback has been adjusted to a value corresponding to the area of

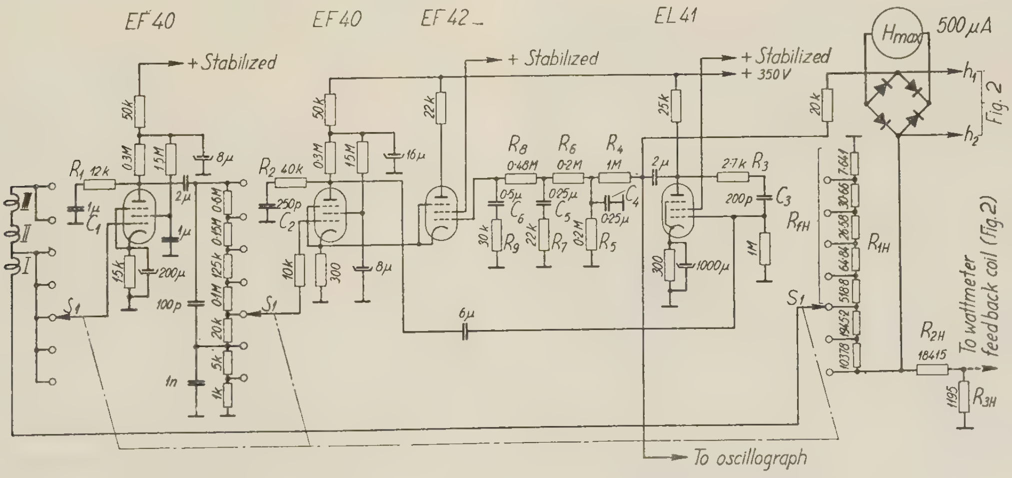


Fig. 10.—Circuit of H_{max} amplifier and measuring instrument.

H_{max} at $f_H = 50$ c/s.

Connection (S_1)	*Full-scale deflection, amp/cm*
1 (uppermost position)	0·1
2	0·5
3	2·5
4	5
5	25
6	100
7	500

The following abbreviations are used:

μ, microfarads.
p, picofarads.
M, megohms.
k, kilohms.
Numerals alone, ohms.

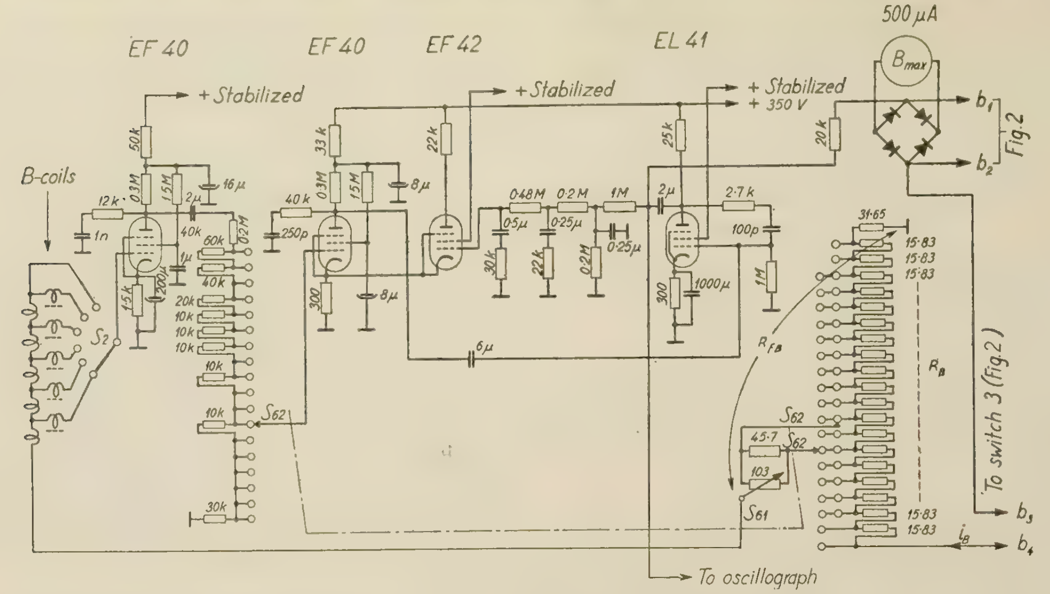


Fig. 11.—Circuit of B_{max} amplifier and measuring instrument.

Connection (s_2)	*Full-scale deflection, Wb/m²*
1 (full clockwise position)	Control
2	0·02
3	0·1
4	0·5
5	2·0
6	10·0

Cross-section setting: 4–40 mm² (S_{61} and S_{62}).

The following abbreviations are used:
μ, microfarads.
p, picofarads.
M, megohms.
k, kilohms.
Numerals alone, ohms.

cross-section of the test specimen. This area is generally determined on the basis of the weight, length and density of the specimen.

The error due to the internal consumption of the rectifier is allowed for in the same way as in the H-amplifier. No other precision components are required in the amplifier, apart from the resistors determining the wattmeter constant and the feedback. Stabilization has been achieved in the same way as in the H-amplifier.

Dimensioning and current reserve of the amplifier are the same as those of the H-amplifier. The sensitivity of the amplifier is highest at the smallest cross-section setting. With full-scale meter deflection, 0·5 mA, the reference input voltage is about 18 mV. However, the power output of the amplifier is insufficient, for instance, with dynamo sheet densities of 1·8–1·9 Wb/m² when the measuring range 2 Wb/m² is used. It is better in such a case to employ the 10 Wb/m² range or a higher cross-section setting.

The combined accuracy of amplifier and instrument is about ±1% of the full-scale deflection.

The instrument has been calibrated at 50 c/s. At other magnetizing frequencies, f_H, its reading must be multiplied by the correction factor $50/f_H$ [cf. eqn. (1)].

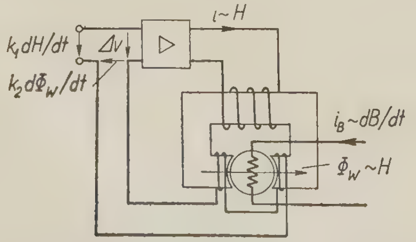


Fig. 12.—Schematic showing the principle of operation of the P_{Fe} measuring instrument.

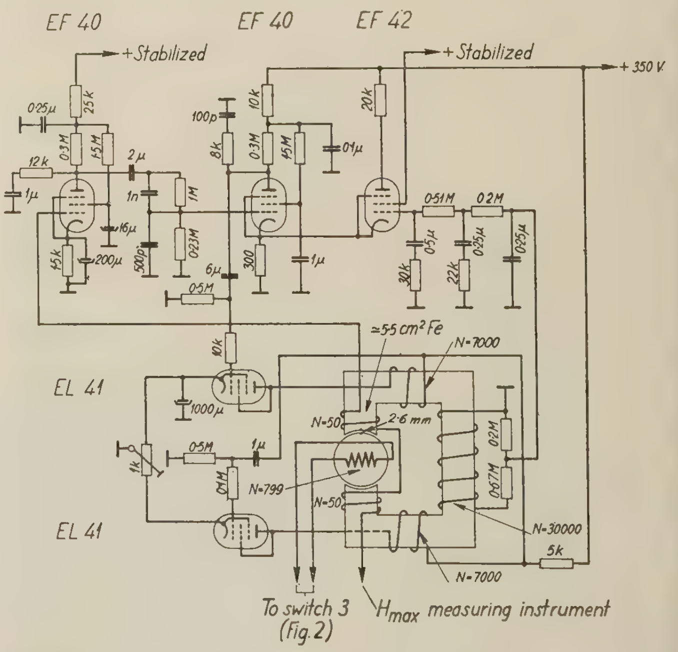


Fig. 13.—Circuit of the P_{Fe} measuring instrument.

The following abbreviations are used:
μ, microfarads.
p, picofarads.
M, megohms.
k, kilohms.
Numerals alone, ohms.

(3.6) P_{Fe} Measuring Instrument

Fig. 12 illustrates the operating principle of the integrating P_{Fe} measuring amplifier, and Fig. 13 shows its circuit diagram.

The feedback voltage is the e.m.f. induced in coils wound on

the pole shoes of the wattmeter, and is very closely proportional to the time derivative $d\Phi_W/dt$ of the flux in the air-gap of the wattmeter. The input voltage being proportional to dH/dt, we may write

$$k_1\frac{dH}{dt} = k_2\frac{d\Phi_W}{dt} + \Delta v$$

With a high feedback coefficient, i.e. when

$$k_2\frac{d\Phi_W}{dt} \gg \Delta v,$$

we have

$$k_1\frac{dH}{dt} = k_2\frac{d\Phi_W}{dt}$$

or

$$\Phi_W \propto H \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (5)$$

The flux in the wattmeter air-gap will thus be proportional to the field strength in the test specimen. The instrument is a fluid-damped spotlight-type instrument with a light-beam pointer 60 cm long.

The sensitivity of the P_{Fe} measuring instrument will be dependent on the B_{max} and H_{max} range settings, and on the position of switch 3 (Fig. 2). A table is provided on the cover of the ferrometer, giving a factor for each H_{max} and B_{max} range combination. This factor has to be further multiplied by a factor C_w, dependent on the position of switch 3, in order to arrive at the final wattmeter constant.

The P_{Fe} measuring instrument has been calibrated to indicate the iron losses per unit mass at $\rho = 7{\cdot}6\,\mathrm{g/cm^3}$. If the sample is of another density, the wattmeter reading must be multiplied by the correction factor $7{\cdot}6/\rho$.

The class-A push-pull amplifier output stage and the magnetizing coils are so connected that the d.c. components of the output-valve anode currents produce opposite magnetization, whereas the alternating components are in phase. The magnetic circuit of the wattmeter will thus only carry a magnetic flux proportional to the alternating component of the magnetic field in the test specimen. However, since it is difficult to make the d.c. components of the current cancel out completely, it is important that the leakage current of the 2 μF condenser in the output stage of the B-amplifier is sufficiently small.

The principles of stabilization are similar to those employed in the H_{max} and B_{max} measuring instruments.

When no test specimen is inserted in the magnetizing device and switch 2 is in the 'CONTROL' position (Fig. 11), the wattmeter reading is dependent on the setting of switch 3 as well as the deflection of the B_{max} and H_{max} instruments and of the resultant error angle of the quantities H and dB/dt fed into the wattmeter. The 'CONTROL' position may thus be used at any time to check the condition of the amplifiers.

The resultant error angles between the magnetic field of the wattmeter and the current through the moving coil amount to $2{\cdot}7'$, $-10'$, $-23'$ and $-1°10'$ at 50, 150, 250 and 750 c/s respectively. Although these error angles are fairly small, they have a remarkable influence at high values of B_{max}. The error arising from them can be calculated exactly if the amplitudes and phase angles of the fundamental and harmonic waves of the B and H curves are known. However, since the determination of phase angles is difficult, the following approximate procedure may be used instead:

The error caused by the phase angle is highest when the phase shift between the H_n and B_n waves, ψ_n, equals 0 or 180°. We can therefore write the relations for the error $(\Delta P_{Fe})_n$ due to the error angle $(\Delta\psi)_n$ of the wave pair H_n, B_n, as follow:

$$\left.\begin{array}{l} |(\Delta P_{Fe})_n| \leqslant K_{PFe} n \hat{H}_n \hat{B}_n |(\Delta\psi)_n| \\ (\Delta P_{Fe})_n > 0 \text{ when } (\Delta\psi)_n > 0 \text{ and } \pi/2 > \psi_n > -\pi/2 \\ \qquad \text{or} \quad (\Delta\psi)_n < 0 \text{ and } \pi/2 < \psi_n < 3\pi/2 \\ (\Delta P_{Fe})_n < 0 \text{ when } (\Delta\psi)_n < 0 \text{ and } \pi/2 > \psi_n > -\pi/2 \\ \qquad \text{or} \quad (\Delta\psi)_n > 0 \text{ and } \pi/2 < \psi_n < 3\pi/2 \\ K_{PFe} = 0{\cdot}0207 \text{ watt/kg when } f = 50\text{ c/s} \\ \qquad\qquad \text{and } \rho = 7{\cdot}6\,\mathrm{g/cm^3} \end{array}\right\} \quad (6)$$

If, for instance, the dH/dt and dB/dt curves corresponding to $1{\cdot}5\,\mathrm{Wb/m^2}$ in Fig. 5A are analysed, we find that

$$H_{max} = 15{\cdot}4\ \text{amp/cm}$$
$$\hat{H}_1 = 8{\cdot}9\ \text{amp/cm} \quad \hat{B}_1 = 1{\cdot}6\ \mathrm{Wb/m^2} \quad \pi/2 > \psi_1 > -\pi/2$$
$$\hat{H}_3 = 4{\cdot}1\ \text{amp/cm} \quad \hat{B}_3 = 0{\cdot}14\ \mathrm{Wb/m^2} \quad \pi/2 < \psi_3 < 3\pi/2$$
$$\hat{H}_5 = 0{\cdot}72\ \text{amp/cm} \quad \hat{B}_5 = 0{\cdot}025\ \mathrm{Wb/m^2} \quad \pi/2 < \psi_5 < 3\pi/2$$

Using the given error angles, this finally yields, according to eqn. (6), the result

$$\sum_{n=1}^{5} (\Delta P_{Fe})_n < (0{\cdot}023 + 0{\cdot}010 + 0{\cdot}0013) = 0{\cdot}0343 \text{ watt/kg}$$

The measured iron losses are $3{\cdot}76$ watts/kg, and the combined error due to the error angles of the fundamental and the third and fifth harmonic is thus less than $+1\%$.

In the preceding example, at least, the error due to the error angles of the fundamental and of the third and fifth harmonic acts in a direction augmenting the deflection of the wattmeter. However, an effect of opposite sign is caused by the saturation of the wattmeter amplifier at the peak of the H-signal. The effect of the saturation phenomenon commences when the B-curve begins to show marked distortion and it can be easily ascertained, e.g. by performing the same measurement at different H_{max} measuring range settings. A typical result is shown below.

	Setting 1	*Setting* 2
B_{max} range	2 Wb/m²	2 W/m²
H_{max} range	25 amp/cm	100 amp/cm
C_w	1	0·2
B_{max}	1·5 Wb/m²	1·5 Wb/m²
H_{max}	15·4 amp/cm	15 amp/cm
P_{Fe}	3·6 watts/kg	3·76 watts/kg

The result of the second measurement, $3{\cdot}76$ watts/kg, is $4{\cdot}45\%$ greater than that of the first measurement. For the 25 amp/cm range the load of the wattmeter amplifier is four times that for the 100 amp/cm range.

It is evident that the wattmeter amplifier has insufficient power output for the consumption of the wattmeter magnetizing coils. There is no doubt that the effect of the error factors resulting from its saturation could be reduced by increasing the ratio of the amplifier output and the power consumed by the wattmeter. Another, possibly better, solution would be to integrate the dB/dt signal and feed the dH/dt signal to the moving coil of the wattmeter, since the B-curve is less peaked than the H-curve.

(4) ACCURACY OF MEASUREMENT OF THE FERROMETER

An absolute calibration of the device has been made, i.e. the constants of its various members have been determined mathematically from their known characteristic values. This is the

only course possible, since experimental calibration with the aid of a standard sheet cannot even be contemplated. Taking into account the errors inherent in the pick-up coils, amplifiers and measuring instruments, the absolute accuracy of the ferrometer, at full-scale deflections of the instruments, is found to be about $\pm 2\%$ for field strength, about $\pm 1\cdot 5\%$ for flux density, and about $\pm 3\%$ for iron losses, when the amplifier outputs are sufficient to provide also for the signal peaks and when the effect of the error angle is negligible.

However, one cannot be fully confident in the reliability of accuracies determined entirely mathematically unless some sufficiently accurate means of comparison is available; it has proved exceedingly difficult to find such a method, and it must be admitted that we have not yet succeeded in measuring the real accuracy of the ferrometer. Even so, it may be of interest to describe some measuring methods and the results arrived at in this way.

(4.1) Comparison of the D.C. and A.C. Curves

The a.c. curve shown in Fig. 14 was determined with the ferrometer in the normal way. The d.c. curve was obtained by

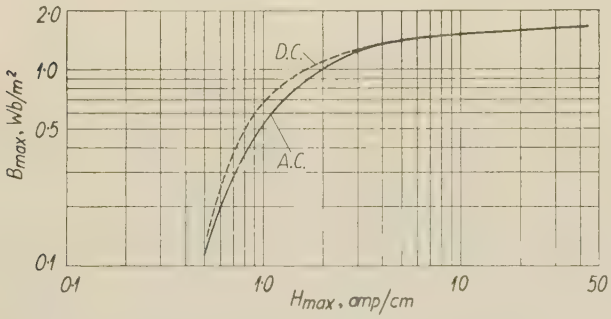


Fig. 14.—Comparison of the d.c. and a.c. magnetization curves.
Test specimen: 0·5 mm thick, 2·2% silicon.

integrating, with the aid of an integrating microvoltmeter, the e.m.f.'s induced in the *B*- and *H*-coils at the reversal of the d.c. field. Both measurements refer to the same test specimen.

At flux densities above $1\cdot 3\,\mathrm{Wb/m^2}$ the a.c. and d.c. curves agree within the limits of accuracy, $\pm 1\%$, of the *B*-amplifier and B_{max} measuring instrument, but in the region of high permeability of the test specimen the peak value of the a.c. flux density remains lower than the d.c. flux corresponding to the same peak field strength. This phenomenon conforms completely, for instance, with Edmundson's[2] observations. Furthermore, according to Brailsford,[3] in the case of high permeability the flux density of the sinusoidally varying total flux on the surface of a dynamo sheet of 0·5 mm thickness may exceed the mean flux density by as much as 25%. Considering that the H_{max} instrument measures the field strength on the surface of the specimen, while the B_{max} instrument records the mean flux density, the discrepancy between the d.c. and a.c. curves is seen to be in full agreement with Brailsford's findings.

It may be mentioned in this connection that the magnetizing curves obtained at, say, 50 and 200 c/s agree perfectly within the limits of accuracy of reading of the measuring instruments.

(4.2) Check of the Measurement of Losses by the Method of Compensation

The loss indication of the ferrometer may be checked in the following way: A test specimen placed in the ferrometer in the standard way is surrounded with a one-layer winding, K, which extends slightly over the boundaries of the section employed in

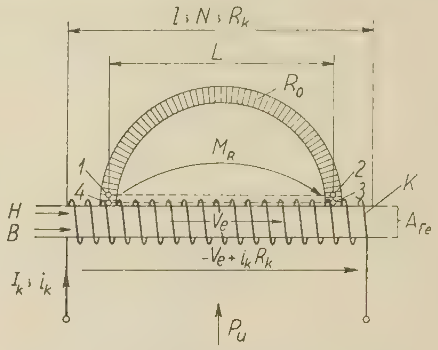


Fig. 15.—Compensation of the iron losses.

the measurement and can be fed with sinusoidal current, I_k, from an external source (Fig. 15). The measurement of the iron losses, P_{Fe}, is first carried out in the standard way at a flux density B(t) with the peak value B_{max} and the corresponding field strength H(t). At this stage $I_k = 0$.

The compensating winding is then supplied with the sinusoidal current I_k in such phase ($\simeq 90°$) with respect to H(t), that the peak flux density resumes its previous value, B_{max}, with unchanged magnetizing current. The effect of I_k upon the *B*-curve is then at its minimum, and if I_k is small, the form of the flux-density curve and the homogeneity in the test specimen will not be changed in any noteworthy degree through the influence of I_k. Consequently, the magnetic flux density is again B(t) and the field strength is H(t).

Making the circuit of the closed curve 1–2–3–4–1 in accordance with Fig. 15, we may write

$$M_R - HL = -Ni_kL/l$$

where the magnetic potentials between points 2 and 3 and between points 4 and 1 have been neglected on account of their small magnitude. Writing this in the form $M_R = L(H - Ni_k/l)$ we can see that the effect of the compensation has been to reduce the mean field strength between the ends of the Rogowski coil by the amount

$$H_k = Ni_k/l \quad . \quad . \quad . \quad . \quad . \quad . \quad (7)$$

The change in losses indicated by the wattmeter should thus be

$$P_k = -\frac{f}{\rho}\int_0^T H_k \frac{dB}{dt}dt \quad . \quad . \quad . \quad . \quad . \quad (8)$$

where ρ is the density of the test specimen. Denoting the e.m.f. induced in the compensating winding by V_e, we have

$$\frac{dB}{dt} = -V_e/NA_{Fe}$$

Substituting in eqn. (8) this expression and that for H_k in eqn. (7), we find that

$$P_k = \frac{f}{lA_{Fe}\rho}\int_0^T V_e i_k dt \quad . \quad . \quad . \quad . \quad . \quad (9)$$

If P_u is the power supplied to the compensating coil, we may write

$$P_u = -f\int_0^T V_e i_k dt + I_k^2 R_k \quad . \quad . \quad . \quad . \quad (10)$$

where R_k is the resistance of the compensating coil. When

$f\int_0^T V_e i_k dt$ is calculated from this equation and substituted in eqn. (9), we obtain for the change in losses

$$P_k = -\frac{P_u - I_k^2 R_k}{l A_{Fe}\rho}$$

and the wattmeter should thus indicate the power

$$P'_{Fe} = P_{Fe} + P_k = P_{Fe} - \frac{P_u - I_k^2 R_k}{l A_{Fe}\rho} \quad . \quad . \quad (11)$$

When I_k is adjusted to make $P'_{Fe} = 0$, i.e. full compensation, the original iron losses should be

$$P_{Fe} = -P_k = \frac{P_u - I_k^2 R_k}{l A_{Fe}\rho} \quad . \quad . \quad . \quad . \quad (12)$$

It is thus possible to calculate the magnitude of the iron losses from this equation of the external power P_u fed into the winding, provided that I_k, l, R_k, A_{Fe} and ρ are known.

The current i_k has been kept sinusoidal so that P_u may then be measured with an a.c. potentiometer. Fig. 16 shows

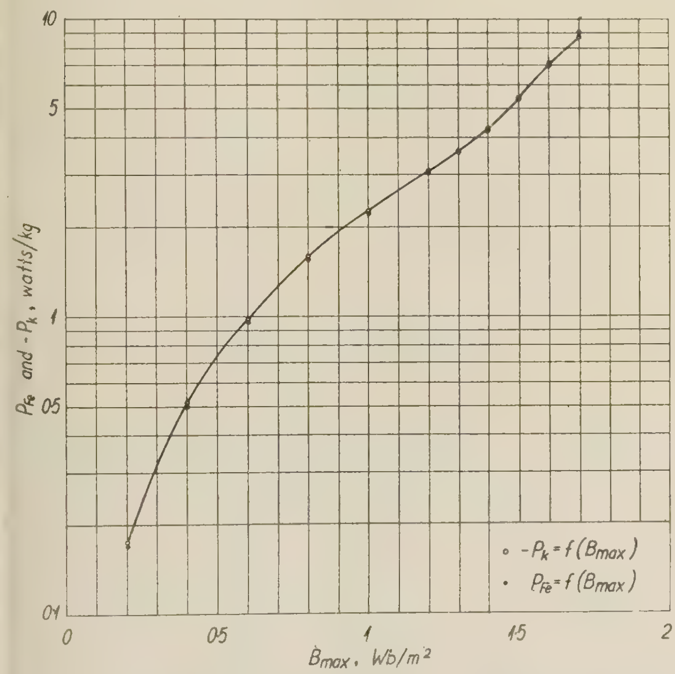


Fig. 16.—$P_{Fe} = f(B_{max})$ and $-P_k = f(B_{max})$ at full compensation.
Test specimen: 0·5 mm thick, 2·2% silicon.

$P_{Fe} = f(B_{max})$ and $-P_k = f(B_{max})$ for full compensation as indicated by the P_{Fe} wattmeter and measured with a particular dynamo sheet. The curves agree within 4% at 0·2–0·8 Wb/m², within 2·5% at 1–1·6 Wb/m² and within 4·5% at 1·7 Wb/m².

The following points should be noted in assessing the results:

(*a*) The current required for compensation was so small at all measured points that no change caused by i_k was observable in the dB/dt curves photographed from the oscillograph.

(*b*) Since the wattmeter of the ferrometer was used as a null instrument, the same error due to error angles is present in P_k and P_{Fe} measured in the standard manner. On the other hand, no errors due to instrument calibration are included in P_k.

(*c*) The highest possible H_{max} range setting was used for measuring P_{Fe} and P_k, in order to avoid saturation of the wattmeter amplifier at the peak of the H-signal. On the other hand, the setting of the H_{max} range was chosen so that the wattmeter could operate as a null instrument with adequate accuracy.

(*d*) P_u and I_k were measured with an a.c. potentiometer of the Campbell–Larsen a.c. type.

(*e*) I_k was derived from a generator with a voltage containing 1·48, 0·645 and 0·725% of the third, fifth and seventh harmonics respectively. For the measurement of P_u and I_k only the contribution of the fundamental was taken into account. If the harmonics of the i_k and dB/dt curves have zero phase-shift, the highest possible error caused by the harmonics can be calculated when their amplitudes are known. Using the values given in Fig. 5B, the maximum error due to the third, fifth and seventh harmonics, $|\Delta(-P_k)|$, is found to be $\leqslant$0·1, 0·2, 0·4, 0·7 and 1·1% at flux densities of 1·3, 1·4, 1·5, 1·6 and 1·7 Wb/m² respectively.

(*f*) The compensating coil was 60 mm long and the B-coils were 44 mm, so that the mean flux density in the region of the compensating coil corresponding to the B-coils was higher than that in the entire compensating coil. This causes an error of less than 1% at $B_{max} > 1$ Wb/m².

(*g*) The length, l, of the compensating coil appears in the denominator of the expression for P_k. Consequently, the error incurred in its measurement, ±0·5%, appears as such in the relative error value of P_k.

Considering the circumstances stated above, it seems that the accuracy of measurement of P_k can be assessed to be ±3%. As can be seen from the previously stated differences of the P_{Fe} and $-P_k$ values of the 2·2% silicon sheet, they remain below the measuring error of P_k, 3%, in the range 1–1·6 Wb/m², and exceed it outside of this range, i.e. at 0·2–1 and 1·6–1·7 Wb/m², by only 1–2%. The measuring error of P_k does not include the error caused by the error angle [see (*b*) above]. However, its effect is probably less than 1% when $B_{max} < 1·5$ Wb/m², as can be concluded from the example calculated in Section 3.6.

(4.3) Comparison with Toroidal Core Measurements

Measurements with the ferrometer have been compared with those obtained by means of toroidal cores, but because of the great differences between the two methods, this comparison does not constitute a reliable basis for evaluating the accuracy of the ferrometer.

In order to achieve sufficient flux, several pieces of sheet material must be stacked together for the toroidal-core measurement. The inhomogeneity thus caused means that the magnetic flux does not always follow the direction of the sheets, as in the ferrometer, but will tend to pass from one sheet to another. It is difficult to account for this phenomenon when comparing the results so obtained with those from the ferrometer. However, since the different parts of the ring present somewhat different magnetic properties, the inclination of the field to the direction of rolling varying between 0 and 90°, the passing of the flux from one sheet to another can be reduced by stacking the sheets so that the rolling directions are coincident. This also facilitates comparison between toroidal-core and ferrometer results, since each cross-section of the ring represents a definite angle between the directions of rolling and of magnetization.

Owing to their inhomogeneity, the flux traversing the cross-section of the toroidal core is unevenly distributed between the sheets. However, the field strength in the different sheets is at least approximately the same at any point of the cross-section in question. It follows that, in the ferrometer measurements, the average $B_{max\,m} = f(H_{max})$ and $P_{Fe\,m} = f(B_{max\,m})$ curves must be determined in the following manner:

$B_{max\,m} = f(H_{max})$ is the arithmetic mean of the $B_{max} = f(H_{max})$ curves measured for the different samples with H_{max} as variable, i.e. each point of the curve is the mean of the B_{max} values measured for the different samples at the same H_{max} value.

$P_{Fe\,m} = f(H_{max})$ is the mean of the $P_{Fe} = f(H_{max})$ curves measured for the different samples with H_{max} as variable.

$P_{Fe\,m} = f(B_{max\,m})$ has been determined from the curves $B_{max\,m}$ $f(H_{max})$ and $P_{Fe\,m} = f(H_{max})$ by elimination of H_{max}.

If the functions $B_{max\ m} = f(H_{max})$ and $P_{Fe\ m} = f(B_{max\ m})$ are known, on the basis of measurements with the ferrometer, for different angles between the directions of magnetization and of rolling, the mean $B_{max\ m} = f(H_{max})$ and $P_{Fe\ m} = f(B_{max\ m})$ curves which should be obtained from the toroidal-core measurement can be calculated and compared with those obtained by measurement. This calculation can suitably proceed from the assumption that the mean magnetic flux density is the same in every cross-section of the ring, provided that the relative permeability of the iron is high.

The rings for this comparison between the $B_{max\ m} = f(H_{max})$ and $P_{Fe\ m} = f(B_{max\ m})$ curves obtained by measurement with the toroidal core and by calculation from the properties of the sheets determined with the ferrometer, as well as the strip samples required for the ferrometer measurements, were prepared from the same sheet by punching, taking them from as closely adjacent parts of the sheet as possible. Strip samples were taken in directions at 0°, 45° and 90° to the direction of rolling of the sheet.

It is then possible, on the basis of the ferrometer measurements, to calculate the $B_{max\ m} = f(H_{max})$ and $P_{Fe\ m} = f(B_{max\ m})$ curves which should be obtained with a regular octagonal ring, and to compare them with the curves measured pertaining to a circular ring. The mean $B_{max\ m} = f(H_{max})$ curve thus calculated for an octagonal ring differs from the d.c. curve measured with the toroidal core in much the same manner as the a.c. curve shown in Fig. 14 differs from the d.c. curve. Fig. 17 shows the mean $P_{Fe\ m} = f(B_{max\ m})$ curves calculated from the ferrometer measurements for the directions of 0°, 45° and 90°; all available samples were measured, the number in each direction being 10, 33 and 23, respectively. The mean iron losses for the octagonal ring are obtained from the equation

$$P_{Fe\ m} = \frac{(P_{Fe\ m})_{0°} + 2(P_{Fe\ m})_{45°} + (P_{Fe\ m})_{90°}}{4}$$

where $(P_{Fe\ m})_{0°}$, $(P_{Fe\ m})_{45°}$ and $(P_{Fe\ m})_{90°}$ are the mean iron losses for 0°, 45° and 90°, respectively, as read from the curves at the same $B_{max\ m}$ value. Some points computed according to this formula have been plotted in Fig. 17. It can be seen that they coincide rather closely with the $P_{Fe\ m} = f(B_{max\ m})$ curve measured with the toroidal core, the greatest difference—about 4%—occurring at 0·115 Wb/m².

In the foregoing some checking methods have been described by means of which we have attempted to determine the measuring accuracy of the ferrometer, although none of these methods is accurate enough to enable a real check to be made. Even so, the results of measurement can be said to show that, with the sheet qualities used and within the ranges investigated, the ferrometer operates at least approximately with the accuracy stated at the beginning of Section 4.

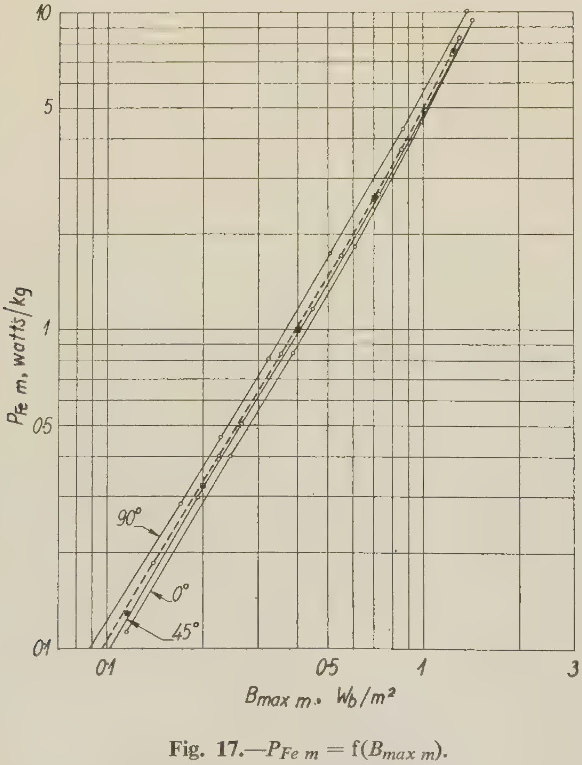


Fig. 17.—$P_{Fe\ m} = f(B_{max\ m})$.

- - - - Obtained from toroidal-core measurement.
——— Calculated from the ferrometer measurements for the directions of 0°, 45° and 90°.
⊡ Computed mean iron losses for octagonal ring.

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[The discussion on the above paper will be found on page 402.]

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DIRECT-READING IRON-LOSS TESTING EQUIPMENT FOR SINGLE SHEETS, SINGLE STRIPS AND TEST SQUARES

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SUMMARY

The paper describes the two basic techniques by which a dynamometer wattmeter may be employed to measure specific iron loss, and shows how these methods have been applied to measurements on Lloyd-Fisher or Epstein samples and to the development of single-sheet testers and single-strip testers. All the equipments give direct readings of flux density, magnetizing force and specific iron loss. The advantages of single-sheet testing are emphasized particularly as a means for grading hot-rolled transformer steel.

LIST OF SYMBOLS

The rationalized M.K.S. system of units is used throughout.

a_H = Area of H-coil.
a_i = Cross-sectional area of iron.
a_y = Cross-sectional area of yoke.
B = Instantaneous value of flux density.
B_{max} = Maximum value of flux density.
D = Wattmeter deflection, scale divisions.
f = Frequency.
H = Instantaneous value of magnetizing force.
H_{max} = Maximum value of magnetizing force.
H_y = Instantaneous value of magnetizing force in yoke.
i = Instantaneous value of current.
I = R.M.S. value of current.
I_{av} = Average value of current.
k = Wattmeter deflection constant, scale divisions per ampere^2.
k_1, k_2, k_3, k_4 = Constants defined by eqns. (3*a*) to (3*d*).
k_5 = B-meter shunting factor.
l_i = Length of iron sample.
l_i' = Effective length of iron sample.
l_y = Length of yoke.
M = Mass of sample.
m = Mutual inductance.
N_1 = Magnetizing turns.
N_2 = Secondary turns.
N_3 = Number of turns in current-transformer primary.
N_4 = Number of turns in current-transformer secondary.
N_H = Number of turns in H-coil.
P_i = Specific iron loss.
R = Resistance.
v = Instantaneous value of e.m.f.
ρ = Density.

(1) INTRODUCTION

Electrical sheet steel is supplied against a guaranteed maximum loss at a specified frequency and flux density. The classical methods of Epstein,[1] Lloyd and Fisher[2] and, more recently, the double-lap Epstein assembly[3] are widely used. Batches of sheet are graded by selecting one or more sample sheets and cutting from these a number of test strips. A considerable amount of calculation is required to obtain the test results. An equipment has been described[4] in which calculating networks are incorporated to reduce this labour.

In transformer-steel applications there is a premium on losses, and this has resulted in the marketing of a number of grades separated by about 7%, which is less than the spread of losses among individual sheets in a batch. The use of a sampling technique will not ensure that individual sheets are correctly graded, and whole batches may occasionally be wrongly graded if the samples happen to be unrepresentative. This situation can be improved by testing and grading sheets individually on a single-sheet tester, provided that the machine is sufficiently fast in operation to keep testing costs to acceptable limits.

In a similar category, although on a much smaller scale, is the single-strip tester. The sheet manufacturer often finds it useful to be able to measure the magnetic properties of single strips of Lloyd–Fisher size or smaller, e.g. to find the distribution of losses over a whole sheet, to compare annealing processes, or to correlate with chemical analyses. The strip tester is essentially a tool of research.

A number of strip testers[5, 6, 7] and single-sheet testers[8, 9] have been described. All of these utilize dynamometer wattmeters for loss measurement, with the exception of Dannatt,[5] who uses an a.c. potentiometer. Some are absolute, but require tedious calculation and correction factors. Others make approximations which can lead to appreciable error. Koppelman[10] and Fiebiger[11] have employed synchronous rectifier methods (the 'ferrometer') to measure the width of the dynamic hysteresis loop, from which the loss is inferred.

More recently, Krug[12] has reviewed German developments in sheet and strip testing and has described briefly the application of dynamometer wattmeters. Wollweber[13] has discussed measurements obtained on Krug's sheet tester, and has compared these with the Epstein method.

The present paper describes developments in Lloyd–Fisher/Epstein, single-sheet and single-strip testers. In each case losses are measured by a dynamometer wattmeter. Calculating networks are included to obtain direct readings of B_{max}, H_{max} and specific loss. No empirical methods are used, the instruments being calibrated in terms of electrical standards. Waveform errors are avoided by using a feedback-amplifier technique, described in a companion paper,[14] to provide sinusoidal flux at the required frequency.

(2) MEASUREMENT TECHNIQUES

(2.1) Loss Measurement Fundamentals

When an iron sample is carried through a closed cycle of magnetization, the energy loss is $\int_c HdB$ or $\int_c BdH$ J/m^3, where $\int_c$ represents the integral over one cycle. Under continuous

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cyclic magnetization at a frequency f cycles per second the iron loss is

$$f\int_c HdB \text{ or } f\int_c BdH \text{ watts/m}^3$$

By changing the variable, this may be written

$$\text{Specific iron loss} = P_i = \frac{f}{\rho}\int_0^{1/f} H\dot{B}dt = \frac{f}{\rho}\int_0^{1/f} B\dot{H}dt \text{ watts/kg} \quad (1)$$

Now a dynamometer instrument carrying instantaneous currents i_a and i_b in its fixed and moving coils experiences an instantaneous torque proportional to $i_a i_b$. Under cyclic conditions the mean torque will be proportional to $f\int_0^{1/f} i_a i_b dt$, and the deflection may be written

$$D = kf\int_0^{1/f} i_a i_b dt \quad (2)$$

where k is a constant.

Comparison of eqns. (1) and (2) shows that the deflection of the dynamometer instrument can be made proportional to specific iron loss if the currents i_a and i_b are made respectively proportional to H and $\dot{B}$, or to B and $\dot{H}$. Thus, if

$$i_a = k_1 H \quad (3a)$$

$$i_b = k_2 \dot{B} \quad (3b)$$

it follows from eqns. (1), (2) and (3) that

$$D = P_i k k_1 k_2 \quad (4a)$$

and this method is considered further in Section 2.2.

Alternatively, if

$$i_a = k_3 B \quad (3c)$$

and

$$i_b = k_4 \dot{H} \quad (3d)$$

$$D = P_i k k_3 k_4 \quad (4b)$$

This method (the H-coil method) is discussed in Section 2.3.

(2.2) Loss Measurement—Magnetizing Current Method

Following eqns. (3a), (3b) and (4a), this method depends on providing wattmeter currents proportional to H and $\dot{B}$ respectively.

(2.2.1) Current Proportional to $\dot{B}$.

In Fig. 1 the voltage induced in the coil N_2 by a flux B in a cross-section a_i is

$$v_2 = \dot{B} a_i N_2 \quad (5)$$

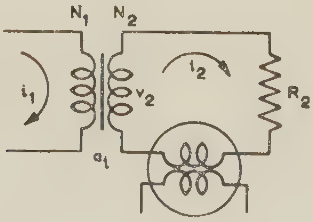

Fig. 1.—Secondary current circuit.

If the circuit is non-inductive, the secondary current is

$$i_2 = \frac{\dot{B} a_i N_2}{R_2}$$

Now the iron cross-section is

$$a_i = \frac{M}{l_i \rho} \quad (6)$$

so that

$$i_2 = \frac{\dot{B}}{\rho}\frac{MN_2}{l_i R_2} \quad (7)$$

Comparing with eqn. (3b), the constant is

$$k_2 = \frac{MN_2}{\rho l_i R_2} \quad (8)$$

Alternatively, as discussed in Section 2.2.3, a feedback amplifier may be interposed to allow the secondary loss $I_2^2 R_2$ to be decreased. In Fig. 2, if the amplifier loop gain is large, the amplifier will function as a conductance $1/R_3$, and the output current will be

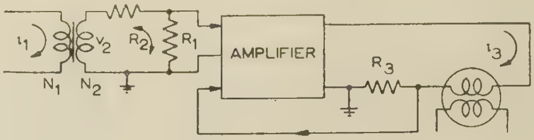

Fig. 2.—Secondary circuit with feedback amplifier.

$$i_3 = v_2 \frac{R_1}{R_2 R_3} \quad (9)$$

From eqns. (5), (6) and (9) the constant in eqn. (3b) is

$$k_2 = \frac{MN_2 R_1}{\rho l_i R_2 R_3} \quad (10)$$

(2.2.2) Measurement of H from Magnetizing Current.

When a uniform magnetic circuit is magnetized from a single evenly distributed winding, the m.m.f. equation is

$$Hl_i = i_1 N_1$$

so that, in eqn. (3a), the constant is

$$k_1 = \frac{l}{N_1}$$

In the more general case the magnetic circuit may consist of two sections: the test sample, and the yoke (in a single-sheet tester), or corner joints (in the Lloyd–Fisher or Epstein testers). In addition, a loaded secondary winding may be applied for measurement purposes. If the yoke or corner-joint field strength and length are H_y and l_y respectively, and if the secondary winding of N_2 turns carries a current i_2, the m.m.f. equation is

$$Hl_i + H_y l_y = i_1 N_1 - i_2 N_2$$

or

$$i_1 = \frac{Hl_i}{N_1}(1 + \alpha + \beta)$$

where

$$\alpha = \frac{H_y l_y}{H_i l_i} \text{ and } \beta = \frac{i_2 N_2}{H_i l_i}$$

Thus

$$k_1 = \frac{l_i}{N_1}(1 + \alpha + \beta) \quad (11)$$

$$= \frac{l_i'}{N_1} \quad (12)$$

where $l_i' = l_i(1 + \alpha + \beta)$.

The wattmeter deflection will depend only on the components of i_1 which are in phase with $\dot{B}$. Thus α can be shown to represent the ratio $\dfrac{\text{iron loss in yoke}}{\text{iron loss in test sample}}$ and β can be shown to represent the ratio $\dfrac{\text{secondary copper loss}}{\text{iron loss in test sample}}$.

(2.2.3) Elimination of Secondary Circuit Loss Errors.

In general, the factor β will vary with the test conditions, and it must be compensated for or reduced to negligible proportions. A compensated wattmeter may be used, as discussed in Reference 4, to eliminate the effect, but, as will be seen in Section 3.1, the circuits employed in the apparatus described require multiple tappings of the primary turns N_1, and this would require corresponding tappings in the compensating coil of the wattmeter, leading to excessive complication.

In the single-sheet tester, the use of a low-consumption portable reflecting wattmeter, combined with the relatively high specimen losses associated with whole sheets, allows the factor β to be reduced to negligible proportions.

In the Lloyd–Fisher and Epstein equipment it is not possible to reduce the wattmeter consumption to negligible proportions. The amplifier method of Fig. 2 is then utilized, allowing the secondary loss $I_2^2R_2$ to be reduced to a negligible amount.

(2.2.4) Elimination of Corner-Loss or Yoke-Loss Errors.

In cases such as the Lloyd–Fisher and Epstein squares, where the corner loss bears a fixed relation to the limb loss, α has a constant value. If β has already been eliminated, eqn. (11) becomes

$$k_1 = \frac{l_i}{N_1}(1 + \alpha)$$

which may be written

$$k_1 = \frac{l_i'}{N_1} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (13)$$

when

$$l_i' = l_i(1 + \alpha) = \text{effective length of test sample} \quad . \quad (14)$$

In the single-sheet tester the yoke dimensions and material are fixed, while the sheet is variable. Thus the factor α changes with each sheet. To eliminate this effect, the yoke is designed to have very low losses which reduce α to negligible proportions.

(2.3) Loss Measurement by *H*-Coil Method

Section 2.2 deals with the case, in single-sheet or strip testing, in which yoke errors can be reduced to negligible proportions, using a low-loss yoke. This condition becomes more difficult to achieve as the size of the test specimen is reduced, and the magnetizing current can no longer be used as a measure of H. Alternatively, it may be preferred to abandon the yoke and use an open-ended specimen. In these cases an H-coil is placed close to the surface of the test sample. The e.m.f. generated in the coil is proportional to $\dot{H}$. An e.m.f. proportional to $\dot{B}$ is obtained from the usual secondary winding.

These e.m.f.'s are applied to current amplifiers, after one of them has been integrated. The amplifiers supply the wattmeter coils with currents proportional to $\dot{B}$ and H, or $\dot{H}$ and B, respectively, as implied by eqns. (4*a*) or (4*b*).

(2.4) Measurement of B_{max} and H_{max}

A rectifier instrument is usually employed to measure the value of B_{max}, which is inferred from the equation

$$\dot{B}_{av} = 4B_{max}f \quad . \quad . \quad . \quad . \quad . \quad . \quad (15)$$

In a similar manner, H_{max} can be inferred from the rectified average value of $\dot{H}$.

(3) APPLICATION TO LLOYD–FISHER AND EPSTEIN METHODS

(3.1) Circuit Analysis

Fig. 3 will be recognized as an extension of the classical iron testing circuit. An amplifier has been interposed between the secondary winding N_2 and the wattmeter coil b, to remove the wattmeter load from the square, as discussed in Section 2.2.3.

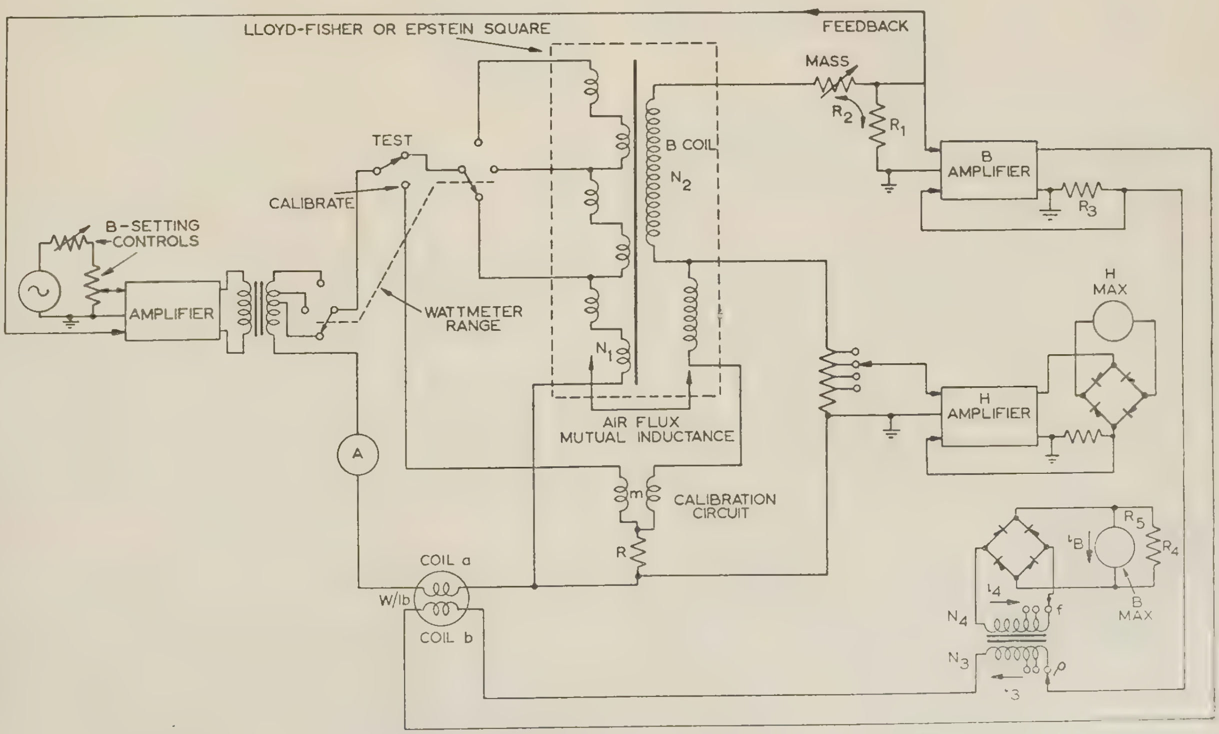

Fig. 3.—Lloyd–Fisher and Epstein square tester.

The values of k_1 and k_2 appropriate to the circuit have been given by eqns. (13) and (10). Substituting these values in eqn. (4*a*) gives the wattmeter deflection

$$D = P_i k\left(M\frac{R_1}{R_2}\right)\left(\frac{N_2}{l_i R_3}\right)\left(\frac{l_i'}{N_1}\right) \quad . \quad . \quad . \quad (16)$$

In Fig. 3, the current i_4 is $i_4 = i_3N_3/N_4$.

Combining this with eqns. (10) and (15), the rectified average value of i_4 will be

$$I_{4av} = k_2\frac{N_3}{N_4}4B_{max}f$$

and the average current through the moving-coil meter R_5 will be

$$I_{Bav} = k_2\frac{N_3}{N_4}4B_{max}fk_5$$

where

$$k_5 = \frac{R_4}{R_4 + R_5} \quad . \quad . \quad . \quad . \quad . \quad . \quad (17)$$

Substituting for k_2 from eqn. (10) gives

$$I_{Bav} = 4B_{max}\left(M\frac{R_1}{R_2}\right)\left(\frac{N_2}{l_i R_3}\right)\left(\frac{f}{N_4}\right)\left(\frac{N_3}{\rho}\right)k_5 \quad . \quad . (18)$$

Examination of eqns. (16) and (18) shows that the wattmeter deflection D will indicate the specific loss P_i, and the current I_{Bav} will indicate B_{max} if each of the factors shown in brackets is held constant. Thus variations in the mass M of successive samples are compensated by making the resistance R_2 proportional to mass. Different frequencies and densities are accommodated by changes in N_4 and N_3 respectively.

In changing over from Lloyd–Fisher to Epstein squares, there is a change in the effective length l_i' of the sample. The squares are wound with appropriate primary turns to make l_i'/N_1 constant on a particular wattmeter range. Wattmeter range switching is provided by changing the primary turns N_1 in the ratio 1 : 2 : 4. Uniform distribution of turns is achieved on each range by utilizing a single-layer quadrifilar winding. A further range can be provided by series/parallel switching of the wattmeter coils, which doubles the constant k in eqn. (16).

Initial adjustment of R_3 enables the required overall wattmeter sensitivity to be attained. When this has been done, the shunt resistance R_4 is adjusted, thus altering the constant k_5, to obtain the required B-meter sensitivity. Calibration circuit requirements are considered in Section 7.

(3.2) Practical Details

The circuit of Fig. 3 shows further details. An oscillator and a feedback amplifier[14] are used to obtain sine-wave induction at standard power frequencies.

A selector switch (not shown in Fig. 3) allows either Epstein or Lloyd–Fisher squares to be used. Each square is equipped with a mutual inductance to compensate for the air flux enclosed by the secondary winding N_2, to avoid errors in B-measurement which become significant at high induction levels. The primary windings of the mutual inductances are distributed in proportion to the magnetizing turns N_1.

The voltage appearing across the mutual-inductance secondary is proportional to the rate of change of magnetizing current, and hence to $\dot{H}$. This voltage is amplified and measured on a moving-coil rectifier meter, which indicates H_{max}. An attenuator at the H-amplifier input provides range switching.

(3.3) Operation

In operation, the test sample is weighed and inserted in the appropriate square. The adjustment for mass is carried out on a 10-turn helical potentiometer which forms part of R_2. The density and frequency controls are set to appropriate values. The B-setting controls (coarse and fine) are adjusted until the B-meter reads the required value, and the test results are read on the wattmeter and the H_{max} meter.

(4) SINGLE-SHEET TESTER

(4.1) Magnetic Circuit Considerations

In considering single-sheet or strip tester requirements, there is an immediate choice between completing the magnetic circuit through the surrounding air, and by providing a laminated yoke. The air-path method encourages non-uniform flux distribution which limits the useful test length to a region in the centre of the sheet or strip. It has been preferred in the present equipments to use a U-shaped laminated yoke to provide a low-reluctance return flux path. Flux uniformity is further improved by splitting the magnetizing coil into a number of parallel sections, some of which are wound on the yoke.

The use of a yoke implies that iron losses will be incurred in the yoke as well as in the sheet. It appears attractive to suppose that the yoke loss will be met by the current flowing in the yoke coils, but preliminary work confirmed that, with close magnetic coupling, the assumption is invalid.

The solution adopted is to measure the total power consumed in sheet and yoke, and to design a yoke in which the loss is reduced to about 1% of the sheet loss. Although this factor varies according to the grade and thickness of the test specimen, it falls within a range of $\pm 0{\cdot}3\%$ for the range of tests envisaged. These yoke requirements are met, in the case of the single-sheet tester, by using about 1½ tons of transformer-steel laminations with a cross-section of 36 × 4 in.

(4.2) Theory

With a yoke in which the losses are reduced to the order of 1%, and a wattmeter for which the pressure circuit losses are negligible in comparison with the sheet loss, the loss measuring circuit takes the form shown in Fig. 4. The appropriate values of k_1 and k_2 are given by eqns. (12) and (8). Inserting these in eqn. (4*a*) the wattmeter deflection will be

$$D = P_i k\left(\frac{M}{R_2'}\right)\left(\frac{N_2}{l_i}\right)\left(\frac{l_i'}{N_1}\right) \quad . \quad . \quad . \quad . \quad (19)$$

Here R_2' is used instead of R_2, and has the significance shown in eqn. (21).

It first appears that an indication of B_{max} can be obtained by measuring the rectified average value of i_2' (Fig. 4). However, the flow of current i_1 through the wattmeter coil *a* induces a quadrature e.m.f. in coil *b*, which in turn causes an unwanted current to be superimposed on i_2'. As this current is in phase quadrature with i_1, it causes no wattmeter deflection; but its magnitude is sufficient to cause errors in a meter placed in series with coil *b*.

B-measurement is therefore carried out by a separate parallel circuit. If the total effective resistance is R_6, the average current in the meter is, by analogy with eqn. (18),

$$I_{Bav} = 4B_{max}\left(\frac{M}{R_6}\right)\left(\frac{N_2}{l_i}\right)\left(\frac{f}{N_4}\right)\left(\frac{N_3}{\rho}\right)k_5 \quad . \quad . \quad (20)$$

Once again, D and I_{Bav} will indicate specific loss and B_{max} respectively if the factors in brackets are held constant. Thus both R_2 and R_6 are varied in proportion to M, while N_2, N_3 and N_4 are tapped in proportion to the sheet length l_i, density ρ and frequency f respectively.

The magnetizing winding N_1 consists of some 30 parallel

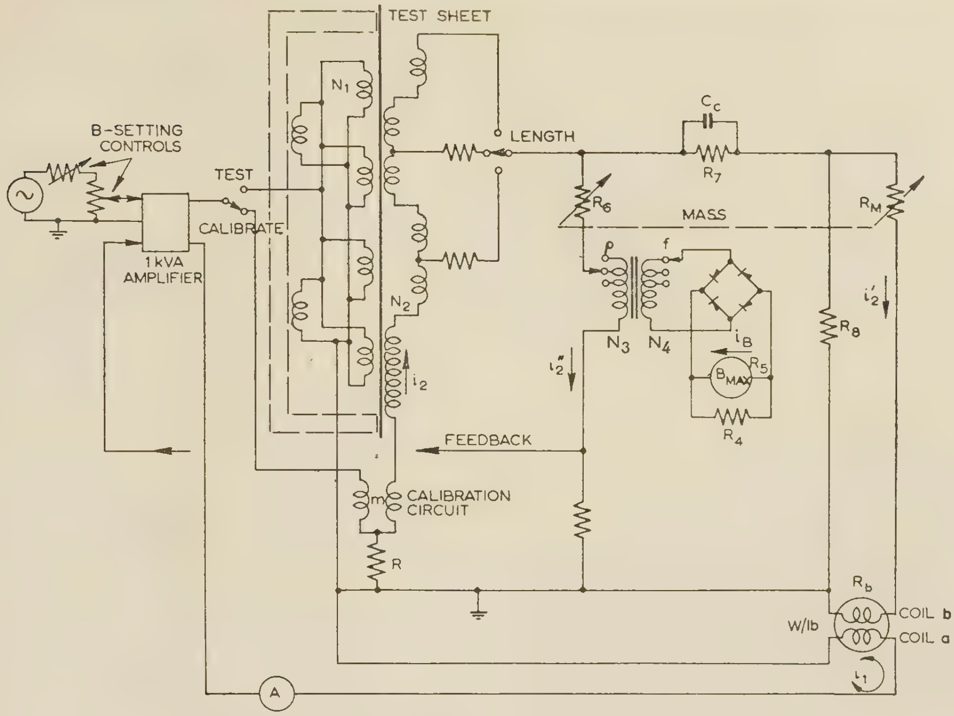

Fig. 4.—Single-sheet tester.

sections, to achieve good flux uniformity. It is not convenient in this case to obtain wattmeter range switching by changing N_1. Instead, three values of the wattmeter deflection constant k are obtained by subdividing the fixed coils of the wattmeter and arranging for series, series/parallel or parallel switching.

Eqn. (19) defines the value M/R_2' when the other constants have been fixed. In the practical arrangement each sheet is placed on a weighing machine before entering the test bed, and the variable part of R_2' is a potentiometer which rotates in synchronism with the weighing machine pointer. For a given weighing machine the required value of R_2' in ohms per degree of rotation is thus defined. Since potentiometers of high linearity are not readily available to close tolerances in ohms per degree, some means of adjustment is necessary. This is provided in Fig. 4 by the addition of resistors R_7 and R_8. R_M is the potentiometer resistance. Here

$$i_2' = \frac{v_2}{R_7 + (R_M + R_b)\left(1 + \frac{R_7}{R_8}\right)} = \frac{v_2}{R_2'} \quad . \quad . \quad (21)$$

so that the actual resistance of the potentiometer is effectively increased by a factor $(1 + R_7/R_8)$. Adjustment of R_8 to obtain the required wattmeter deflection under calibration conditions thus allows for some tolerance in the calibration (ohms per degree) of R_M.

(4.3) Details

Referring to Fig. 4, the sheet and yoke are magnetized by parallel-connected coils from an amplifier of 1 kVA rating. Two versions of the test bed have been built. One takes sheets of 8 ft minimum length, of which a 7 ft length is tested. The other takes sheets of 6 ft minimum length, of which a 5 ft length is tested. Since the secondary winding N_2 is tapped to accommodate greater length, the air-flux coils are correspondingly tapped. Resistance equalizers are added to maintain constant total resistance.

Phase-angle compensation is added, in the form of the capacitance C_c, to annul the effect of inductance in the wattmeter voltage branch. The adjustment is described in Section 7.

As the speed of operation is limited by the settling time of the wattmeter, a relay has been used (not shown in Fig. 4) to insert the calibration components into circuit, between sheet tests, at a current level which deflects the wattmeter to about 80% of full-scale deflection.

(4.4) Operation

In operation, a sheet is first placed on the weighing machine, where the mass-setting resistors are adjusted. It is then loaded automatically into the test bed, where a trip switch causes rubber-faced clamps to maintain good magnetic contact between the ends of the sheet and the pole faces, and at the same time switches on the magnetizing current. After initial adjustment, the B-setting is automatic unless the sheet density or length is altered. The instrument operator determines the sheet grade from the wattmeter reading, and presses a button which ejects the sheet at the output end and indicates the grade to the unloading operator. Meanwhile another sheet has been weighed and now enters the test bed. An appropriate relay control system ensures sequential operation.

Speeds of 300–400 sheets per hour are achieved over an 8-hour shift.

(5) SINGLE-STRIP TESTERS

It is noted in Section 2.3 that as the size of strip specimen is diminished it becomes increasingly difficult to design a yoke of negligible loss. This difficulty led to the use of H-coils in the instrument described in Section 5.1. As developed, this system required the use of an additional integrator and amplifier in the H-channel, and two additional magnetizing amplifiers.

With subsequent experience it became evident that a nickel-iron yoke could meet the low-loss requirement, and a second version of the strip tester, similar in its circuits to the Lloyd–Fisher/Epstein equipment, was built. It is described in Section 5.2.

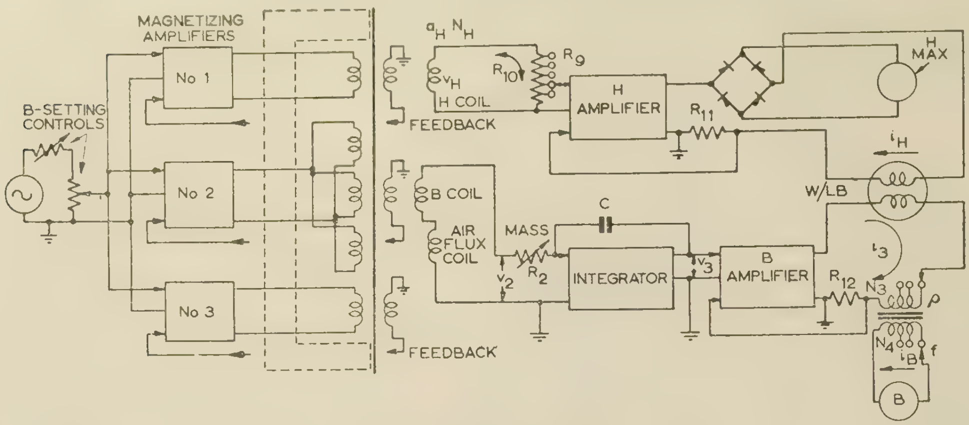

Fig. 5.—Single-strip tester.

(5.1) *H*-Coil Method

(5.1.1) Theory.

The application of the *H*-coil method is shown in Fig. 5. Here the $\dot{B}$ signal is integrated, and the wattmeter is supplied with currents proportional to B/ρ and $\dot{H}$ respectively. It would be equally permissible from the point of view of loss measurement to integrate the $\dot{H}$ signal. The method adopted has the advantage that H_{max} can be measured directly by a rectifier meter.

Integration is obtained by means of an amplifier with a *CR* feedback network. With a high amplifier gain, the output/input relation becomes (Fig. 5)

$$\frac{v_3}{v_2} = \frac{1}{pCR_2} \quad . \quad . \quad . \quad . \quad . \quad . \quad (22)$$

where *p* is the differential operator.

Also

$$i_3 = \frac{v_3}{R_{12}} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (23)$$

Using eqns. (5), (6), (22) and (23), the wattmeter current i_3 is

$$i_3 = \frac{N_2MB}{l_i\rho CR_2R_{12}} \quad . \quad . \quad . \quad . \quad . \quad (24)$$

This gives for the constant k_3 as defined in eqn. (3*c*)

$$k_3 = \frac{N_2M}{l_i\rho CR_2R_{12}} \quad . \quad . \quad . \quad . \quad . \quad (25)$$

The *H*-coil voltage is

$$v_H = 4\pi 10^{-7}N_Ha_H\dot{H} \quad . \quad . \quad . \quad . \quad (26)$$

The wattmeter current is

$$i_H = v_H\frac{R_9}{R_{10}R_{11}} \quad . \quad . \quad . \quad . \quad . \quad (27)$$

From eqns. (26) and (27) the constant k_4 as defined in eqn. (3*d*) is

$$k_4 = 4\pi 10^{-7}N_Ha_H\frac{R_9}{R_{10}R_{11}} \quad . \quad . \quad . \quad . \quad (28)$$

From eqns. (4*b*), (25) and (28), the wattmeter deflection will be

$$D = P_ik\left(\frac{M}{CR_2}\right)\left(\frac{R_9}{R_{10}}\right)\left(\frac{N_2}{l_iR_{12}}\right)\left(\frac{4\pi a_HN_H10^{-7}}{R_{11}}\right) \quad . \quad (29)$$

The current i_B is

$$i_B = B\left(\frac{M}{CR_2}\right)\left(\frac{N_2}{l_iR_{12}}\right)\left(\frac{N_3}{\rho N_4}\right)k_5 \quad . \quad . \quad . \quad (30)$$

and the rectified average value of i_H is

$$I_{Hav} = H_{max}4f\left(\frac{R_9}{R_{10}}\right)\left(\frac{4\pi a_HN_H10^{-7}}{R_{11}}\right) \quad . \quad . \quad (31)$$

Eqns. (29), (30) and (31) show that D, i_B and I_{Hav} will be proportional to specific loss, B and H_{max} respectively, if the factors shown in brackets are constant. Thus R_2 is varied in proportion to M, and N_4 is varied in inverse proportion to ρ. Stepped values of R_9/R_{10} provide wattmeter and *H*-meter range switching. Initial adjustment of R_{11} to achieve the required *H*-meter sensitivity is followed by adjustment of R_{12} for wattmeter sensitivity, and then of k_5 (i.e. R_4) for *B*-meter sensitivity, under appropriate calibration conditions.

It will be noted that i_B is at every instant proportional to B [not to $\dot{B}$ as in eqns. (18) and (20)]. As B is sinusoidal, it is permissible to use either an r.m.s. meter or an average meter, calibrated to read B_{max}.

(5.1.2) Constructional Details.

The coil unit has been constructed in two sizes. One takes 25 × 7 cm (Lloyd–Fisher) strip, and the other takes 15 × 2 cm strip, a size which has been found more convenient for metallurgical experiments on a laboratory scale. The strip is placed in a narrow slot which is surrounded by the *B*-coil, while the *H*-coil is wound in two parts, above and below the strip and as close to it as practicable. The magnetizing winding, in a number of parallel sections, is wound over the *B*- and *H*-coils. The U-shaped yoke is arranged to move vertically to provide clamping pressure between the pole faces and the ends of the strip under test.

Dannatt[5] has noted that the accuracy of loss measurement by *H*-coil methods depends on whether the field sampled by the *H*-coil is truly representative of the field at the surface of the iron. In the present tester, field uniformity has been improved by splitting the magnetizing coils into three sections, each energized from a separate amplifier. The system is fully described elsewhere.[14]

(5.2) Low-Loss Yoke Method

The use of a laminated Mumetal yoke of 7 × 2 cm cross section reduces yoke losses to about 1% of the strip loss, which enables the magnetizing current to be passed through one wattmeter coil as a measure of *H*. The high degree of field uniformity required by the *H*-coil method is no longer necessary. This allows the two additional magnetizing amplifiers to be removed. Parallel connection of the sections of the magnetizing coil, combined with the low reluctance of the yoke, provides an adequate degree of flux uniformity along the length of the specimen.

In other respects the method is similar to that of the single-sheet tester, except that the instruments are coupled through an amplifier to the secondary winding N_2. Thus the circuit is basically identical with the Lloyd–Fisher/Epstein circuit (Fig. 3). An H-coil, amplifier and rectifier meter are included to measure H_{max}.

(5.3) Performance of Single-Strip Testers

Both forms of strip tester are similar in operation to that described in Section 3.3. A testing rate of about one strip per minute is achieved, being limited mainly by the speed of weighing on a chemical balance.

In addition to its obvious use in assessing the results of chemical composition, annealing and processing, the single-strip tester has also been used for studies such as the pattern of loss over the area of whole sheets. By applying a tensile stress to a strip already in the coil unit, the effects of strain have been measured. Some effects of shearing strains are recorded in Fig. 6. These were obtained by measuring the properties of a strip, shearing it in half longitudinally, reassembling the two parts in the tester and measuring again. The shearing process was then repeated, cutting the strip into four and then into eight.

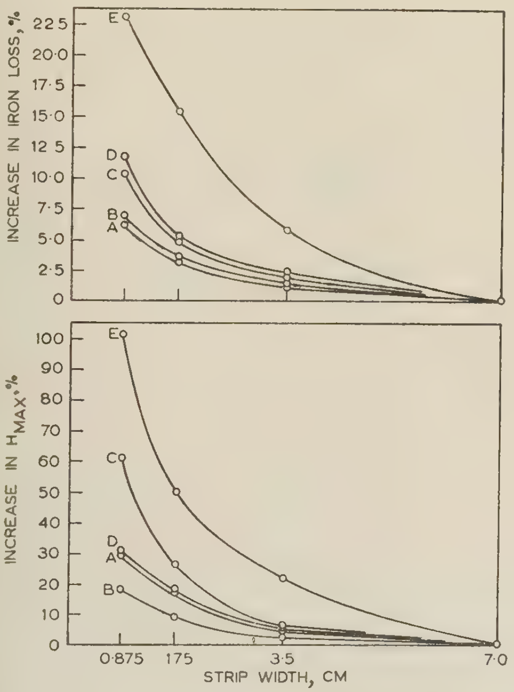

Fig. 6.—Effect of shearing on the iron loss and H_{max} of single Lloyd–Fisher strips at a fixed B_{max}.

All tests at $B = 1 \cdot 3\,\mathrm{Wb/m^2}$ and $f = 50\,\mathrm{c/s}$

Curve	Silicon content	Thickness	Measured characteristics for 0·07 m width	
			Loss	H_{max}
	%	in	W/lb	AT/m
A	0·3	0·025	2·65	406
B	0·3	0·018	2·05	430
C	2·5	0·020	1·29	390
D	4·0	0·014	0·93	537
E	3·0 grain-oriented	0·012	0·52	71·6

(6) AMPLIFIER REQUIREMENTS

In the instruments described, an overall accuracy of better than 1% has been the aim. Wherever possible, individual components have been designed and adjusted to the order of 0·1%.

The power factor encountered in testing non-oriented transformer sheet at $1 \cdot 3\,\mathrm{Wb/m^2}$ is about 0·2. This means that the wattmeter currents differ from phase quadrature by about 12°, and a phase error of about 0·012° will cause a wattmeter error of 0·1%. The problem, therefore, is to design an amplifier which will supply the necessary wattmeter current with a maximum phase shift, over the required range of working frequencies, of the order of 0·01°.

Fig. 7 is a chart of the type discussed by Nichols,[15] showing

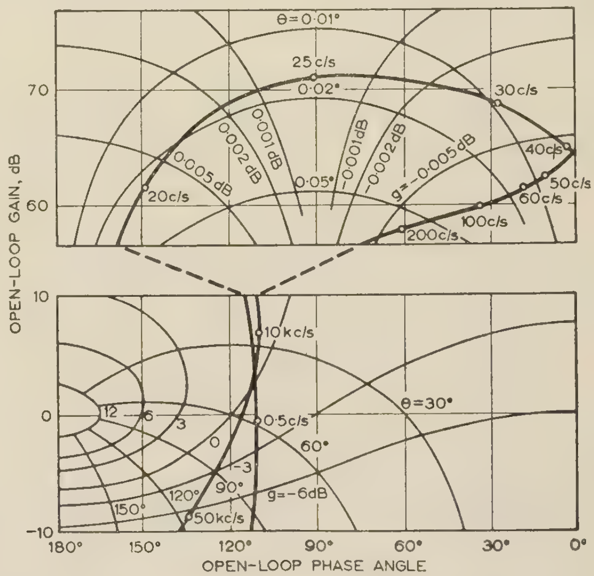

Fig. 7.—Closed-loop/open-loop transfer chart showing locus of B-amplifier response.

g = Closed-loop gain.
θ = Closed-loop phase angle.

the relation between the closed-loop and open-loop gain and phase angle of a feedback amplifier. This shows that the 0·01° target can be achieved with an open-loop gain of 60 dB and a phase shift not exceeding 10°, or 70 dB and 33° respectively. With 60 dB of feedback, the closed-loop gain will drop by only 0·01 dB or 0·1% if the amplifier gain is halved, and is therefore very insensitive to mains voltage and valve ageing effects. Fig. 7 also contains a plot of the performance actually obtained with the amplifier shown in detail in Fig. 8. This amplifier is used in

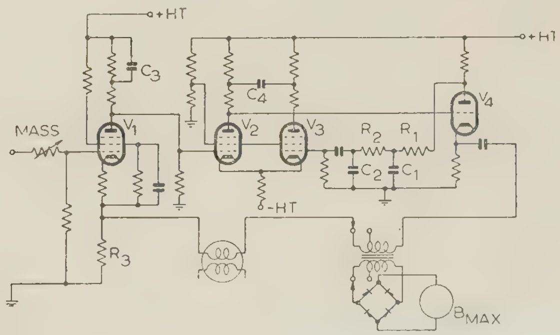

Fig. 8.—B-amplifier used in Lloyd–Fisher/Epstein test equipment.

the Lloyd–Fisher/Epstein test equipment, and supplies a current of about 7 mA with an input signal of 2 volts.

The amplifier consists essentially of two pentode stages V_1 and V_2 and a cathode-follower V_4, which supplies current to the wattmeter and the B-meter. Overall feedback is applied through resistor R_3. Stabilization of this amplifier, utilizing 60 to 70 dB of negative feedback, presents a considerable problem, particularly at the low-frequency end. A rapid rate of attenuation at frequencies below 25 c/s is achieved by a subsidiary feedback loop applied through the time-constants R_1C_1 and R_2C_2 and the valve V_3, which is coupled through a common cathode resistance to V_2. Capacitors C_3 and C_4 are provided for high-frequency stabilization of the main and subsidiary feedback loops respectively.

Similar design criteria apply to the integrating amplifier used in the strip tester (Fig. 5).

(7) CALIBRATION

Initial calibration and periodic checks of instrument accuracy are performed by diverting the primary current from the magnetizing windings to a four-terminal resistance R and mutual inductance m. These components, and the calibration current I_{cal}, are chosen to produce wattmeter and B-meter deflections appropriate to particular values of P_i and B_{max} respectively.

As applied to the circuits of Figs. 3 and 4, the wattmeter deflection condition implies that

(Power dissipated in R)

$$= (\text{Power dissipated in iron under test}) \times \frac{N_2}{N_1}$$

i.e.
$$I_{cal}^2R = P_iM\frac{l_i'}{l_i}\frac{N_2}{N_1} \quad . \quad . \quad . \quad . \quad . \quad (32)$$

The B-meter deflection condition implies that the voltage appearing across the secondary terminals of the calibration circuit shall be equal to the voltage which would be generated across N_2;

i.e.
$$I_{cal}\sqrt{[R^2 + (2\pi fm)^2]} = \frac{\sqrt{(2)}\pi B_{max}MN_2f}{l_i\rho} \quad . \quad . \quad (33)$$

Eqns. (32) and (33) may be solved either by selecting a convenient value for I_{cal} and calculating R and m, or by putting $m = 0$ and solving for I_{cal} and R. The first method is usually preferred, because it permits the constants to be selected to give simultaneous full-scale deflections of the ammeter, wattmeter and B-meter. A similar calibration technique is applicable to the circuit shown in Fig. 5.

In the single-sheet tester (Fig. 4) the leakage inductance of the secondary winding N_2 and the wattmeter coil inductance cause the current i_2' to lag behind the $\dot{B}$ voltage. This is compensated by a small capacitance C_c connected across the resistance R_7. Initial adjustment of C_c is carried out by disconnecting the calibration resistance from the circuit and passing current through the mutual inductance. C_c is adjusted to give zero wattmeter deflection, indicating that the wattmeter currents are exactly in phase quadrature. Similar techniques are used in setting up the equipments shown in Figs. 3 and 5.

(8) COMPARISON OF TEST METHODS

(8.1) Lloyd–Fisher, Epstein and Single-Strip Methods

Table 1 shows the results of comparisons between single-strip, Lloyd–Fisher and Epstein tests on the same material. In each case a pack of about 20 strips was tested in the Lloyd–Fisher square, and the results compared with the mean of individual tests in the strip tester. The 25 × 7 cm Lloyd–Fisher strip was then sheared to give two 25 × 3 cm strips (the left-over 25 × 1 cm strip being rejected). These strips were tested in a non-standard double-lap Epstein square (the standard size is 28 × 3 cm). Each pair of 25 × 3 cm strips was then joined with adhesive tape to form a 25 × 6 cm strip, and these were tested in the Lloyd–Fisher square for comparison with the Epstein figures. The Lloyd–Fisher figures, before and after shearing, also gave a measure of the sheared edge effect.

From the data given in Table 1, the following conclusions may be drawn:

(a) Agreement between the single-strip and Lloyd–Fisher tests is generally better than 2%.

(b) Lloyd–Fisher tests show that shearing strain introduced in cutting 7-cm strips down to 3 cm in width causes an average loss increase of 3·5% for non-oriented material and 6·9% for oriented material.

(c) A comparison of the Lloyd–Fisher and Epstein methods on 3 cm strips shows that, on average, the Epstein reads between 1 and 2% low. This effect must presumably be attributed to corner-joint effects.

(d) The effects of shearing strain and of corner joints, noted in (b

Table 1

STATISTICAL COMPARISON OF IRON-LOSS MEASUREMENTS BY LLOYD–FISHER, EPSTEIN AND SINGLE-STRIP METHODS, BASED ON MEASUREMENTS ON 10 SAMPLES, EACH CONTAINING INITIALLY 20 STRIPS 25 × 7 CM

Material	Average Lloyd–Fisher loss		Error relative to Lloyd–Fisher test: 25 × 7 cm single-strip (i)	2 at 25 × 3 cm Lloyd–Fisher (ii)	25 × 3 cm Epstein (iii)	Error (iii) relative to (ii)
	W/lb		%	%	%	%
0·014 in non-oriented transformer steel	0·829	Mean	0·48	3·48	2·09	−1·39
B_{max} = 1·3 Wb/m²		Range to include 95% of observations	±1·03	±2·54	±1·57	±1·15
0·013 in grain-oriented transformer steel	0·602	Mean	−1·71	6·91	5·20	−1·71
B_{max} = 1·5 Wb/m²		Range to include 95% of observations	±1·74	±5·04	±3·90	±1·56

and (c) respectively, combine to make the 25 × 3 cm Epstein test high by an average of 2·1%, compared with the standard 25 × 7 cm Lloyd–Fisher test, for non-oriented transformer steel.

The corresponding figure for grain-oriented material is 5·2%. Errors of this magnitude do not, of course, occur in routine testing of oriented material, because the test strips are strain-relieved by annealing after shearing.

(8.2) Artificial Standard Sheet

In assessing the performance of the single-sheet tester, several difficulties are encountered. In the first place, no standard sheet of known loss is available for checking accuracy. An artifice for increasing sheet losses has been devised involving a uniform winding applied closely round the tested length of sheet, and loaded with a variable resistance and an r.m.s. ammeter. The sheet loss P_i is first measured, at the desired flux density, with the winding on open-circuit. Subsequently the winding is loaded with various values of resistance R. The current I flowing in R is noted in each case, and also the wattmeter indication P_i'. If M' is the mass of iron tested, the specific loss of the sheet has been increased by I^2R/M'. The test is used to confirm agreement between the independent methods of power measurement, i.e. that $P_i' - P_i = I^2R/M'$. Agreement within 0·5% has been obtained.

(8.3) Comparison of Lloyd–Fisher and Single-Sheet Grading

Differences between Lloyd–Fisher and single-sheet methods may be expected for four reasons:

(a) The act of shearing a whole sheet may relieve some locked-up stresses and may introduce further stresses close to the sheared edge. Both of these effects may change the losses and permeability.
(b) The strip sample is not necessarily representative of the whole sheet, but only of those parts from which it is cut.
(c) Some additional loss is caused in the sheet test by eddy currents circulating in the plane of the sheet, caused by the normal component of flux transferring from the sheet to the yoke. (This is discussed in Section 12.2.)
(d) The sheet test is performed with magnetization entirely in the longitudinal or rolling direction, while B.S. 601 calls for Lloyd–Fisher or Epstein samples containing equal numbers of strips cut parallel to and at right angles to the rolling direction (or, alternatively, all cut at 45° thereto).

The overall effect of factors (a), (b) and (c) has been measured by comparing single-sheet tests with longitudinal Lloyd–Fisher samples cut from the sheets. Results of a number of these tests are shown in Fig. 9(a), from which it is seen that, although some scatter or variability exists, the sheet tests are on average 4% higher than the Lloyd–Fisher. Most of this difference is due to the factor (c). The variability is such that 95% of the points fall within a range of error of ±4·7%.

When Lloyd–Fisher samples containing equal numbers of longitudinal and transverse strips are compared with single-sheet results, as in Fig. 9(b), the variability is increased to ±5·5%, owing to variation in the ratio of losses measured in the two directions. On average, the losses measured by both methods are now almost equal, which implies that the increase in sheet loss due to factors (a), (b) and (c) is equal to the increase in Lloyd–Fisher loss caused by the introduction of transverse strips. For thicker sheets and for sheets of lower resistivity (lower silicon content) factor (c) increases. Grading limits for single-sheet testing are therefore adjusted to ensure that, over any bulk of material, the same percentage is passed into each grade by either method.

Since it is the usual practice to cut transformer laminations parallel to the rolling direction, the single-sheet test provides a measure of the most significant property. When sheets are individually graded in this way considerable uniformity of quality can be expected. The only factors likely to cause variation between the properties of the sheet and of transformer laminations cut from them are the stresses relieved and imposed by shearing.

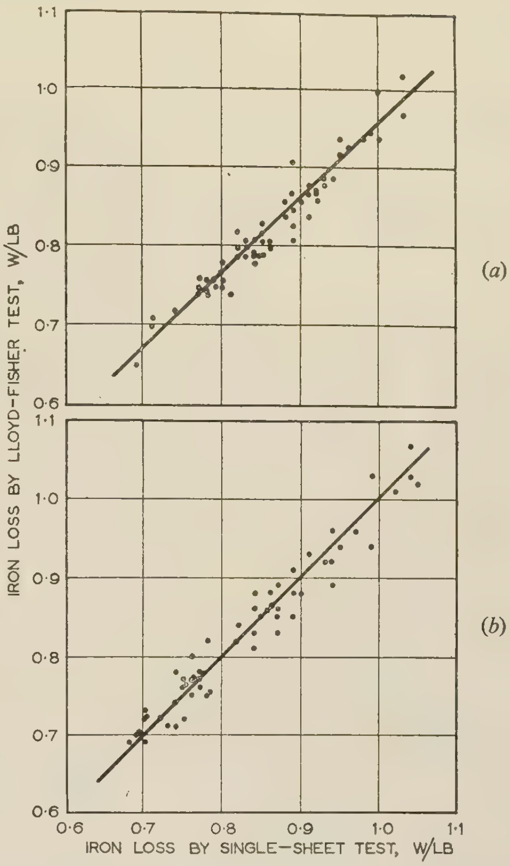

Fig. 9.—Comparison of iron loss by Lloyd–Fisher and single-sheet methods.
(a) Longitudinal strips.
(b) Longitudinal and transverse strips.

Lloyd–Fisher or Epstein testing, on the other hand, represents a low sampling rate, and cannot ensure uniformity among all the sheets in the sampled batch.

(9) CONCLUSIONS

Application of the principles outlined in the paper has resulted in the development of a series of equipments for test squares, single sheets and single strips, all of which have the merit of providing direct readings and overall accuracy of the order of 1%.

Differences between Lloyd–Fisher and Epstein tests are largely explained by the effects of shearing strains, causing the Epstein to give higher loss figures. Agreement within 2% is obtained between Lloyd–Fisher and single-strip tests.

The loss measured by the single-sheet tester refers to magnetization in the rolling direction, and cannot agree individually with the standard-square methods, though when applied to non-oriented transformer sheet it is considered to offer a much more satisfactory method of grading than can be achieved by squares.

(10) ACKNOWLEDGMENTS

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(12) APPENDICES

(12.1) Effective Length of Overlap between Sheet and Yoke

In the single-sheet tester described in Section 4, it was found necessary to employ a yoke of 4 in width, to minimize yoke losses and reluctance. If the sheet makes good magnetic contact with the yoke the flux transfer may be expected to take place close to the point X in Fig. 10. If for any reason an air-gap appears between the sheet and yoke the flux transfer is spread

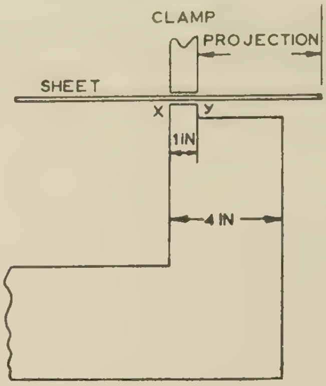

Fig. 10.—Joint between sheet and yoke.

between X and Y, and the effective sheet length is increased. To prevent any appreciable variation in the effective sheet length, the pole face XY has been reduced to 1 in. In the worst case, if a relatively large gap appears, the flux transfer will occur in a linear manner between X and Y. If the sheet loss varies as B^2, the loss curve is parabolic and the effective sheet length can readily be shown to have increased by 1/3XY, i.e. 1/3 in in a tested length of 60 in in the case of the 6 ft tester, or 0·55%. In the unlikely event of large gaps appearing simultaneously at both ends, the error cannot exceed 1·1%.

(12.2) Errors due to Normal Component of Flux Passing from Sheet to Pole Face

In passing out of the sheet into the pole face, the normal component of flux generates eddy currents in the plane of the sheet. The eddy-current path resistance can be increased, and hence the losses can be reduced, if the sheet projection (Fig. 10) is reduced to zero, but in practice a variable projection is necessary for sheets of different length. The problem does not lend itself to exact calculation.

The effect on the measured iron-loss of varying sheet length was obtained by tests on long sheets which were steadily diminished in length by cutting equal amounts off each end, so that the same part of each sheet was retained in the tester. Typical results are given below for a 4% silicon 0·014 in transformer sheet at 1·3 Wb/m² and 50 c/s.

Sheet length, in	62	66	72	84	96	Lloyd–Fisher strips
Relative iron loss	100·8	102·2	103·1	103·9	104·0	100·0

The tests at 62 in had zero projection. The normal range covered by this tester is 72 to 96 in. Within this range the error is reasonably constant—between 3 and 4%. The error is larger for lower-silicon grades because of their lower resistivity, but is still reasonably constant within the range of tested lengths. Due allowance for this error is made by methods discussed in Section 8. In a recent paper Krug[12] has utilized a double yoke to reduce errors due to the normal component of flux. The method has the disadvantage of additional mechanical complexity, since the upper yoke has to be raised before removing a sheet.

[The discussion on the above paper will be found on page 402.]

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THE CONTROL OF FLUX WAVEFORMS IN IRON TESTING BY THE APPLICATION OF FEEDBACK AMPLIFIER TECHNIQUES

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SUMMARY

By the use of a feedback amplifier technique, the voltage appearing across a search coil wound on a ferromagnetic core can be made to follow any desired driving waveform, and the inherent harmonic distortion effects arising from non-linear B/H characteristics are reduced by an amount approximating to the loop gain. The technique is applied in iron-testing circuits and to a.c. bridges to obtain sinusoidal flux waveforms, thereby avoiding errors in loss measurement which would otherwise arise from distortion effects. Three amplifiers are employed in a single-strip tester to obtain uniform magnetization over the length of the strip.

LIST OF SYMBOLS

The rationalized M.K.S. system of units is used throughout.

a_i = Cross-sectional area of iron.
B = Instantaneous value of flux density.
B_{max} = Maximum value of flux density.
b_n, b'_n = Harmonic content defined in eqns. (8) and (15).
f = Frequency.
G = Amplifier voltage gain.
H_{max} = Maximum value of magnetizing force.
h_n = nth harmonic content of magnetizing current.
I = R.M.S. value of current.
i = Instantaneous value of current.
k = Transformer turns ratio, N_2/N_1.
l_i = Length of iron.
M = Mass.
R = Resistance.
t = Time.
V = Voltage.
v = Instantaneous value of voltage.
Z = Impedance.
ϕ = Phase angle.
ρ = Density.
ω = Angular frequency.

(1) INTRODUCTION

When a ferromagnetic material is cyclically magnetized from an a.c. source, the non-linear relationship between B and H results in the generation of harmonic voltages, not present in the source, causing distortion of voltage and flux waveforms. The amount of distortion increases as saturation values are approached, and also as the impedance of the magnetizing circuit increases. The problem has been analysed by Astbury[1] and Macfadyen.[2]

Since certain a.c. magnetic properties—the eddy-current loss, the hysteresis loss and hence the magnetizing force—are frequency-dependent, measurements of these properties will be affected by the harmonic content present in the flux waveform, and hence by the circuit impedance. In the measurement of iron losses it has in the past been customary to specify a heavy magnetizing winding and an alternator of large capacity to reduce these impedances, so that ultimately the wattmeter coil impedance is a limiting factor. In the case of single-sheet and single-strip testers having closed magnetic circuits, the magnetizing coil resistance and leakage inductance cannot be reduced to values which allow of low flux distortion. Shenck[3] and Cormack[4] have described techniques by which the phase and amplitude of harmonic generators, connected in series with the magnetizing windings, were manually adjusted to balance harmonics.

Flux distortion can be limited automatically by the use of a feedback-amplifier technique. The amplifier receives a driving voltage of the required waveform from an oscillator or signal generator, and an opposing feedback voltage from a search winding or B coil surrounding the ferromagnetic core. The magnetizing current is provided by the amplifier output. If the loop gain is high, the feedback voltage will follow the driving voltage in magnitude and waveform. The technique, first mentioned by one of the authors,[5] was subsequently used by Greig and Shurmer[6] for dual-frequency excitation, by Jackson, Melville and Sewell[7] for non-sinusoidal waveforms, and by George.[8] A detailed analysis of the problem is given in the following pages.

The feedback technique enables not only the waveform but also the amplitude of B to be controlled. It is therefore possible, by using more than one amplifier, to maintain uniform induction at various parts of a magnetic circuit. This scheme finds application in the design of a single-strip tester, where three amplifiers are employed to maintain the required flux at the centre and ends of the strip.

A further application is to a.c. bridge measurements of ferromagnetic properties. Several writers[9, 10, 11] have referred to the fact that iron losses inferred from measurements at fundamental frequency are subject to errors arising from harmonic distortion. A recent paper by Cooter and Harris[12] describes a method for computing these errors. The present paper shows that, if sinusoidal flux is achieved, virtually no error exists, and good agreement is obtained between bridge and wattmeter methods.

The descriptions contained herein of the application of the feedback technique to single-sheet and single-strip testers, and to Lloyd-Fisher or Epstein squares, are complementary to the companion paper,[13] which describes loss-measuring techniques.

(2) DISTORTION THEORY

(2.1) Sine-Wave Generator

In Fig. 1, an iron sample is magnetized by a current i flowing through a coil of N turns from a generator giving a voltage v. Z represents the combined impedance of the generator and the

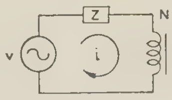

Fig. 1.—Magnetizing circuit for iron sample.

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copper resistance and leakage reactance of the coil. The circuit equation is

$$v = iZ + Na_i\dot{B} \quad . \quad . \quad . \quad . \quad . \quad . \quad (1)$$

In the particular case where v is sinusoidal, i.e.

$$v = V_{max} \sin \omega t \quad . \quad . \quad . \quad . \quad . \quad . \quad (2)$$

the magnetizing current i will contain a fundamental component and a series of harmonics imposed by the non-linear B/H loop characteristics, and may be written

$$i = I_1 \sin (\omega t + \phi_1) + \Sigma I_n \sin (n\omega t + \phi_n) \quad . \quad . \quad (3)$$

Substitution from eqns. (2) and (3) in eqn. (1) gives

$$Na_i\dot{B} = V_{max} \sin \omega t - Z_1 I_1 \sin (\omega t + \phi_1) - \Sigma Z_n I_n \sin (n\omega t + \phi_n) \quad . \quad (4)$$

where Z_1 and Z_n are the values of Z appropriate to fundamental and nth harmonic frequencies respectively. This may be expanded in the form

$$Na_i\dot{B} = E_{max} \sin (\omega t + \theta) - \Sigma Z_n I_n \sin (n\omega t + \phi_n) \quad . \quad (5)$$

where $E_{max} = \sqrt{[(V_{max} - Z_1 I_1 \cos \phi_1)^2 + (Z_1 I_1 \sin \phi_1)^2]}$. (6)

and $$\theta = \arc\tan \left(\frac{Z_1 I_1 \sin \phi_1}{V_{max} - Z_1 I_1 \cos \phi_1}\right) \quad . \quad . \quad . \quad (7)$$

Thus the $\dot{B}$ wave contains harmonic components not present in the generator voltage. From eqn. (5) the nth harmonic content of the $\dot{B}$ wave is

$$b_n = \frac{n\text{th harmonic amplitude}}{\text{fundamental amplitude}} = \frac{Z_n I_n}{E_{max}} \quad . \quad . \quad (8)$$

(2.2) Feedback Amplifier

In Fig. 2, an oscillator with output voltage v' is employed to drive an amplifier of gain G which supplies current to the mag-

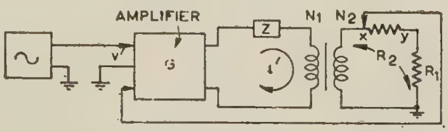

Fig. 2.—Feedback amplifier control of flux waveforms.

netizing coil N_1. Negative feedback is applied to the amplifier input from a search coil N_2. It is assumed that the search coil is closely wound around the iron sample so that no air flux is included, or, alternatively, that an air-flux coil has been used to provide compensation. The circuit equation becomes

$$G(v' - N_2 a_i\dot{B}) = i'Z + N_1 a_i\dot{B} \quad . \quad . \quad . \quad . \quad (9)$$

$$N_1 a_i\dot{B}(1 + Gk) = Gv' - i'Z \quad . \quad . \quad . \quad . \quad (10)$$

where $$k = \frac{N_2}{N_1}$$

If v' and i' have the forms expressed in eqns. (2) and (3), respectively, eqn. (10) becomes

$$N_1 a_i\dot{B} = \frac{1}{1 + Gk}[GV'_{max} \sin \omega t - Z_1 I'_1 \sin (\omega t + \phi'_1) - \Sigma Z_n I'_n \sin (n\omega t + \phi'_n)] \quad . \quad (11)$$

$$= E'_{max} \sin (\omega t + \theta') - \frac{1}{1 + Gk} \Sigma Z_n I'_n \sin (n\omega t + \phi'_n) \quad . \quad . \quad . \quad . \quad (12)$$

where

$$E'_{max} = \frac{1}{1 + Gk}\sqrt{[(GV'_{max} - Z_1 I'_1 \cos \phi'_1)^2 + (Z_1 I'_1 \sin \phi'_1)^2]} \quad . \quad . \quad . \quad . \quad (13)$$

and $$\theta' = \arc\tan \left(\frac{Z_1 I'_1 \sin \phi'_1}{GV'_{max} - Z_1 I'_1 \cos \phi'_1}\right) \quad . \quad . \quad (14)$$

In eqn. (12) the nth harmonic content of the $\dot{B}$ wave is

$$b'_n = \frac{Z_n I'_n}{E'_{max}} \frac{1}{1 + Gk} \quad . \quad . \quad . \quad . \quad . \quad (15)$$

Comparison of eqns. (8) and (15) gives

$$\frac{n\text{th harmonic content with feedback}}{n\text{th harmonic content without feedback}} = \frac{b'_n}{b_n} = \frac{I'_n E_{max}}{I_n E'_{max}} \frac{1}{1 + Gk} \quad . \quad . \quad . \quad . \quad (16)$$

In most of the cases of interest, the series impedance Z in Fig. 1 is not large enough to cause I_n to differ greatly from I'_n. (Both approximate to the values appropriate to sinusoidal flux waveforms for the particular core material.) Also E_{max} and E'_{max} will be approximately equal, and, if $Gk \gg 1$,

$$\frac{b'_n}{b_n} \simeq \frac{1}{Gk} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (17)$$

i.e. the effect of feedback is to reduce the harmonic content of the $\dot{B}$ wave by a factor approximating to the feedback loop gain Gk. By making this factor sufficiently large, the harmonics may be reduced to any desired level.

(2.3) Amplitude and Frequency Stability

If $Gk \gg 1$, $GV'_{max} \gg Z_1 I'_1$ and $GV'_{max} \gg Z_n I'_n$, eqn. (11) gives the expected feedback-amplifier result

$$N_2 a_i\dot{B} \simeq V'_{max} \sin \omega t \quad . \quad . \quad . \quad . \quad . \quad (18)$$

i.e. the feedback and drive voltages approach equality. The frequency and amplitude stability of the $\dot{B}$ wave will therefore be as good as those of the driving source, which is conveniently a valve oscillator. Thus, in addition to reduction of waveform distortion, the feedback method provides a means for obtaining stable B-amplitude and frequency, which greatly facilitates accurate measurements.

Where tests of similar material are carried out at a high repetition rate, as in single-sheet testing, it is desirable that the driving voltage V'_{max} should not require adjustment for each successive sheet, in spite of variations in the cross-section a_i.

Now $$a_i = \frac{M}{l_i \rho} \quad . \quad . \quad . \quad . \quad . \quad . \quad (19)$$

so that for a fixed length l_i and density ρ, a_i is proportional to mass. If a potentiometer is introduced by moving the feedback point from x to y in Fig. 2, the feedback voltage becomes $N_2 a_i(R_1/R_2)\dot{B}$, which gives, using eqns. (18) and (19),

$$\frac{N_2 R_1}{l_i \rho} \frac{M}{R_2} \dot{B} = V'_{max} \sin \omega t \quad . \quad . \quad . \quad . \quad (20)$$

Thus $\dot{B}$, and hence B, will be of constant amplitude if R_2 is varied in proportion to the mass M.

(3) AMPLIFIER REQUIREMENTS

(3.1) Degree of Feedback required

Writing $$h_n = \frac{I'_n}{I'_1} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (21)$$

and

$$Z_i = \frac{E'_{max}}{I'_1} = \text{iron impedance at fundamental frequency} \quad . \quad (22)$$

eqn. (15) becomes $$b'_n = \frac{Z_n}{Z_i}\frac{h_n}{1 + Gk} \quad . \quad . \quad . \quad . \quad . \quad (23)$$

This equation may be used to estimate the loop gain factor Gk necessary to achieve a required level of b'_n. Fig. 3 shows typical

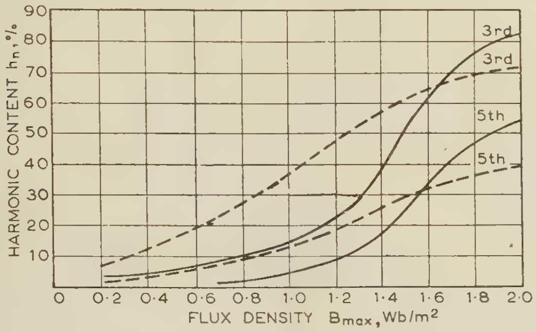

Fig. 3.—Harmonic content h_n of H wave under sine-wave B conditions expressed as a percentage of fundamental H.

.... 4% non-oriented silicon-iron.
———— 3% grain-oriented silicon-iron.

values of the magnetizing-current or H-wave harmonic content h_n for grain-oriented and non-oriented transformer steels magnetized with sinusoidal induction at 50 c/s. The third harmonic content is largest and lies between 0·5 and 1 at high induction levels (Fourier analysis considerations suggest that, for a magnetizing current waveform which approaches a delta shape at high flux densities, h_n cannot exceed unity). For example, for oriented material at 1·7 Wb/m², h_3 may be taken as 0·7 and h_5 as 0·4.

The impedance Z will consist largely of the amplifier output impedance. If triode output valves are used, and if they are matched into the load impedance Z_i, the output impedance may be of the order of $0{\cdot}5Z_i$. Making some additional allowance for other circuit components, such as wattmeter coils and ammeters, and for the resistance and leakage inductance of coil N_1, the total series impedance Z_n may be about $0{\cdot}7Z_i$.

A target for low waveform distortion may be taken as 1% third harmonic, i.e. $b'_n = 0{\cdot}01$. Inserting appropriate values in eqn. (23) gives $Gk = (0{\cdot}7 \times 0{\cdot}7)/0{\cdot}01 - 1 = 48$ at third-harmonic frequency. Similarly $Gk = 27$ at fifth-harmonic frequency.

If pentode output valves are used, the amplifier output impedance may be about five times the load impedance, so that $Gk = 350$ for 1% third harmonic and $Gk = 200$ for 1% fifth harmonic. These values are necessarily approximate, but they indicate the order of loop gain required.

(3.2) Amplifier Rating

Under sine-wave magnetization at high induction levels, the ratio of maximum to r.m.s. magnetizing current is high. Since a power amplifier output will be limited by maximum rather than r.m.s. current, the maximum value is used as a criterion.

The maximum value of current in a coil of N turns is

$$I_{max} = \frac{H_{max} l_i}{N}$$

and the r.m.s. voltage across the load is $V = \sqrt{(2)}\pi B_{max} a_i N f$. The equivalent r.m.s. amplifier rating is

$$\text{r.m.s. } VA/\text{kg} = \frac{V\dfrac{I_{max}}{\sqrt{2}}}{\rho a_i l_i} = \pi B_{max} H_{max}\frac{f}{\rho} \quad . \quad . \quad (24)$$

Typical values for oriented and non-oriented transformer steel are given in Table 1. At high induction levels, other silicon-steel grades fall between these extremes. In calculating amplifier ratings, due allowance must be made for other impedances in the magnetizing circuit, and for output transformer losses, etc.

Table 1

APPROXIMATE AMPLIFIER RATINGS IN VOLT-AMPERES PER KILOGRAMME FOR $f = 50$ c/s

B_{max}	4% silicon non-oriented $\rho = 7\,550$ kg/m³		3% silicon grain-oriented $\rho = 7\,630$ kg/m³	
	H_{max}	Rating	H_{max}	Rating
Wb/m²	AT/m	VA/kg	AT/m	VA/kg
1·0	147	3·06	26·2	0·54
1·3	525	14·2	35·1	0·94
1·5	2 900	90·5	53·5	1·65
1·8	14 600	500	525	19·2
2·0	47 800	1 990	6 440	265

(4) APPLICATIONS

(4.1) Lloyd–Fisher and Epstein Testing

An amplifier of 150 VA rating is used in routine testing equipment for loss measurements, at flux densities up to 1·5 Wb/m², on samples of up to 1 kg of non-oriented transformer steel. Fig. 4 compares harmonic contents of the flux waveforms, obtained by this method, with those obtained using a 1 kVA

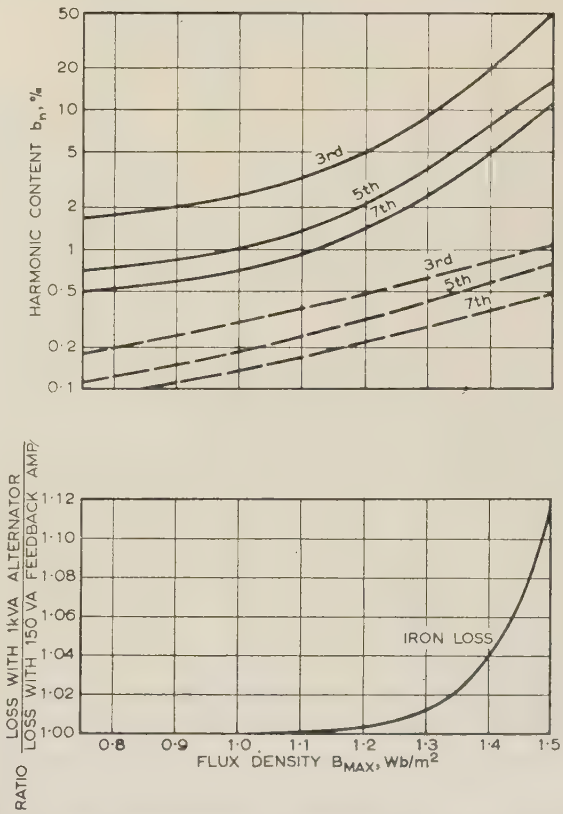

Fig. 4.—Lloyd–Fisher square. Comparison of harmonic content of B-wave and of iron loss using 1 kVA alternator and 150 VA amplifier.

———— 1 kVA alternator.
.... 150 VA feedback amplifier

alternator as a power source. The curves also indicate the corresponding change in iron loss. In this instance, the distortion arising from the alternator circuit is sufficient to cause an iron loss increase of 12% at $1 \cdot 5\,\text{Wb/m}^2$.

(4.2) Loss Testing at High Flux Densities

Errors in iron loss measurement at high flux densities arise both from waveform distortion and, if dynamometer wattmeters are used, from low power-factor limitations. Waveform distortion errors have been overcome by means of a 1 kVA feedback amplifier of a design outlined in Section 10.1. This has enabled samples of about $0 \cdot 5$ kg to be excited at 50 c/s to flux densities of $2 \cdot 0$ and $2 \cdot 2\,\text{Wb/m}^2$ for hot-rolled and grain-oriented transformer steel respectively, with less than 1% third harmonic in the $\dot{B}$ waveform.

Phase shift errors arising from inductance in the wattmeter voltage-coil circuit can be overcome by interposing a suitable amplifier between the B coil and the wattmeter, as discussed elsewhere.[13] The principal limitation of accuracy arises from the small wattmeter deflections obtainable at very low power factors. Using a particular laboratory reflecting wattmeter, an estimated accuracy of $\pm 2\%$ was attained when the power factor fell to $0 \cdot 01$, corresponding to flux densities of $1 \cdot 8\,\text{Wb/m}^2$ for hot-rolled transformer steel and $2 \cdot 2\,\text{Wb/m}^2$ for grain-oriented material.

(4.3) Single-Sheet Testing

Single-sheet testing necessarily implies that the magnetizing coils enclose one thickness only of sheet, compared with about five strips in each limb of a Lloyd–Fisher square, or still more in an Epstein square. Moreover, the need for adequate clearance for conveying sheets into the test position results in a magnetizing coil of two or three times the height of the corresponding Lloyd–Fisher coil, which increases the leakage inductance correspondingly. These factors have the effect of reducing E_{max} and increasing Z_n in eqn. (8), so that the magnitudes of harmonics in the $\dot{B}$ wave are increased about ten times. [Eqn. (8) applies, and therefore this argument holds, only if a closed magnetic circuit is used, and does not apply to open-ended sheet testers.] In the single-sheet tester described in Reference 13, which employs a magnetic yoke, reasonable waveforms could not be attained without a feedback amplifier—in this case one of 1 kVA rating.

(5) MULTIPLE AMPLIFIERS FOR UNIFORM FLUX DISTRIBUTION

Dannatt[9] has pointed out, in discussing a single-strip tester, that the accuracy of loss measurement by H-coil methods depends on whether the field sampled by the H coil is truly representative of the field at the surface of the iron. This was reaffirmed during the development of a single-strip tester described in Reference 13. As the H coil is relatively bulky, and cannot conveniently be placed very close to the surface of the iron, it was desirable that the field enclosed by the magnetizing windings should be truly uniform. In d.c. permeameter practice it is usual to employ compensating coils near the ends of the test strips, and to make manual adjustments of the currents in these coils until uniform conditions are achieved. Shenck[3] has described a similar manual technique in his sheet and strip testers.

In the present tester, automatic compensation has been achieved with an extension of the feedback-amplifier excitation method. Three amplifiers are driven from a common source (Fig. 5). One of these supplies current to the central parallel magnetizing coils. Each amplifier receives a feedback voltage from a search coil wound under the appropriate magnetizing coil. In operation each amplifier supplies the current necessary to maintain the voltage in its search coil at the required value. Thus B is automatically maintained at a constant value at three points representing the ends and the centre of the strip.

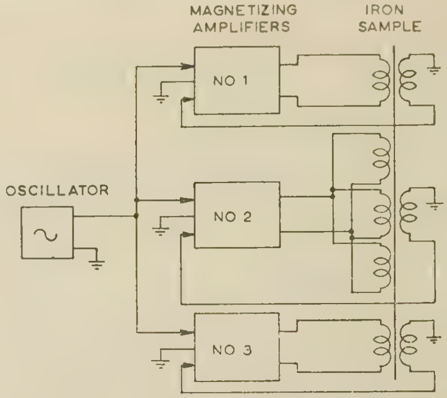

Fig. 5.—Self-compensating system of three amplifiers for single-strip tester.

Fig. 6 shows typical results obtained when this system is compared with the uncompensated system. Over the length covered by the H coils, the uniformity of magnetizing current is improved, particularly for high-permeability material [Fig. 6(a)]. It is inferred that the H-coil field strength is more truly representative of the surface value of the field.

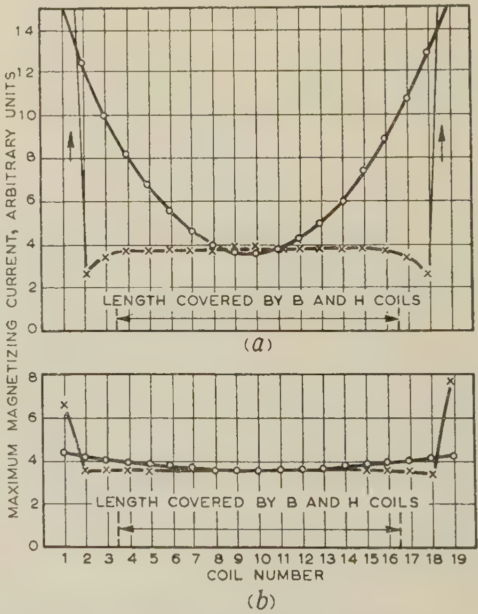

Fig. 6.—Distribution of current in single-strip tester magnetizing coils: $B = 1 \cdot 3\,\text{Wb/m}^2$; $f = 50$ c/s.

(a) Grain-oriented transformer steel.
(b) Non-oriented transformer steel.
—○—○— 19 coils in parallel.
—×—×— 17 coils in parallel; end coils excited by separate amplifiers.

Two practical difficulties may be mentioned. First, since all three search coils are coupled by a common magnetic circuit, the feedback voltage to each amplifier is considerably influenced by the output currents of the two others, and a parallel operation problem arises. Instability was cured by the use of separate h.t. power supplies to output stages, and by limiting the feedback to a lower value (35 dB) than was originally intended. Secondly, the inevitable error of the feedback amplifier implies that some

phase shift exists between the search-coil feedback voltage and the oscillator drive voltage. As the central and end amplifiers were not identically loaded, the phase shift differed, which implied that the B waveform changed in phase between centre and ends. A test showed that a 1° phase difference caused a 7% error in the strip-tester loss measurements, and revealed the need for accurate phase alignment of the amplifiers. This was achieved by resistance-capacitance circuits between the oscillator and each amplifier.

(6) A.C. BRIDGE MEASUREMENTS

Several writers[9, 10, 11] have drawn attention to various aspects of the behaviour of a circuit consisting of a linear impedance and a non-linear impedance (in the present context an iron-cored coil) in series with a sine-wave voltage generator. Harmonic currents, required by the non-linear impedance, flow through the linear impedance, which therefore consumes power at each harmonic frequency. Analysis shows that harmonic power terms, of identical magnitude but opposite sign, appear in the non-linear impedance. This impedance may be regarded as an energy convertor, absorbing power supplied by the sine-wave generator at fundamental frequency and converting some of it to harmonic frequencies for absorption in the linear impedance. The magnitude of the non-linear impedance as measured at fundamental frequency is therefore dependent on the linear impedance, and is a function of the circuit in which it is measured.

Dannatt[9] has applied this analysis in so far as it refers to iron-loss measurements, and has shown that a correcting factor $R(I_3^2 + I_5^2 + I_7^2 + \ldots)$ representing the harmonic power dissipated in the resistive part R of the linear impedance, must be subtracted from the apparent fundamental power in the iron core, to obtain the true iron loss. Cooter and Harris[12] have shown experimentally the correctness of this analysis, and have applied it to bridge measurement of iron losses at flux densities up to $1 \cdot 5\,\mathrm{Wb/m^2}$, by measuring the harmonic currents I_3, I_5, I_7, etc. The process of measurement and calculation is tedious.

By applying the feedback technique to the bridge, as in Fig. 7,

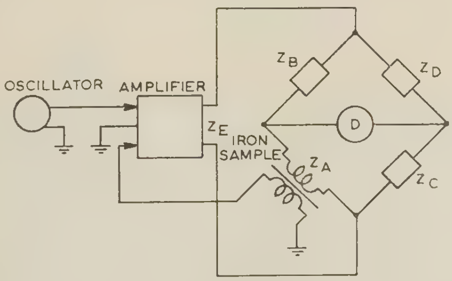

Fig. 7.—A.C. bridge with flux waveform control.

the flux waveform in the iron core can be made to approximate closely to a sine wave. Harmonic voltages continue to appear across the linear impedance, here formed by the series arm Z_B, the amplifier output impedance Z_E and the copper resistance of Z_A. (It is assumed here that Z_C and Z_D have sufficiently high values to be ignored; if not, they modify the effective value of Z_E.) These harmonic voltages are countered by corresponding voltages appearing at the amplifier output terminals. Thus harmonic power consumed in the circuit is now supplied by the amplifier, and measurements at fundamental frequency of the impedance of the iron-cored coil by normal a.c. bridge or potentiometer methods are not dependent on other circuit components. This technique allows measurements of true iron loss to be made, without further correction, at flux densities up to about $1 \cdot 3\,\mathrm{Wb/m^2}$. Astbury's modification[1] of the Heydweiller bridge is preferred, because it yields the iron losses directly, without any corrections for copper losses in the magnetizing windings.

At very high flux densities the harmonic distortion becomes so severe that, even with the application of 40 dB of feedback, it is no longer possible to reduce the flux waveform harmonics to levels at which the harmonic power is negligible. Fig. 8 illustrates

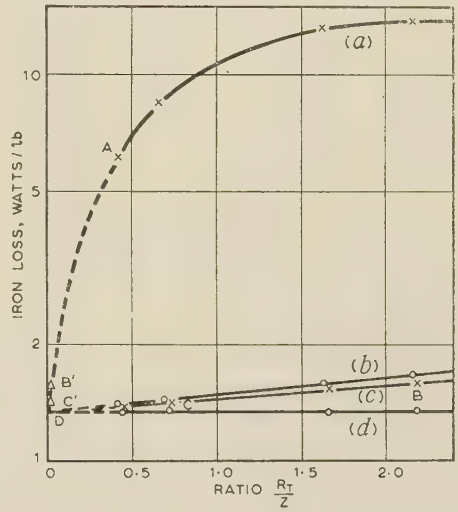

Fig. 8.—Comparison of bridge and wattmeter measurements of iron loss on a sample of 4% silicon hot-rolled transformer steel.

$B = 1 \cdot 7\,\mathrm{Wb/m^2}$. $f = 50\,\mathrm{c/s}$. Mass $= 344\,\mathrm{g}$.

(*a*) Bridge measurement—no feedback.
(*b*) Wattmeter measurement—no feedback.
(*c*) Bridge measurement—40 dB negative feedback.
(*d*) Wattmeter measurement—40 dB negative feedback.

results of a test to measure the effectiveness of the feedback method in bridge measurements on 4% silicon hot-rolled transformer steel at a flux density of $1 \cdot 7\,\mathrm{Wb/m^2}$. Maxwell's bridge was used, and a wattmeter was connected into the arm containing the iron-cored coil, in the manner used by Cooter and Harris,[12] to provide a comparison between bridge and wattmeter measurements. The curves show the measured iron loss plotted against the ratio R_T/Z, where R_T is the total series resistance in the magnetizing circuit (excluding the effective resistance of the iron), and Z is the impedance of the iron at fundamental frequency. Curves (*a*) and (*b*) were obtained using a sine-wave voltage source across the supply terminals of the bridge. The apparent iron loss is many times the true iron loss, even at point A, which represents the lowest attainable value of R_T. Curve (*b*) shows how the true iron loss, indicated by the wattmeter, increases with R_T. This is due to the increased eddy-current loss arising from harmonic distortion.

When the bridge was excited by a 1 kVA power amplifier using 40 dB of negative feedback, curves (*c*) and (*d*) were obtained. The flux waveform was now nearly sinusoidal, and the wattmeter readings [curve (*d*)] were constant. The iron loss indicated by the bridge balance conditions [curve (*c*)] still differs from the true value [curve (*d*)], although the error is greatly reduced compared with curve (*a*). A measure of the true iron loss can readily be obtained by balancing the bridge for two values of R_T, obtaining points such as B and C, and extrapolating to D.

From another viewpoint, the application of negative feedback may be considered to reduce the series resistance by an amount approximating to the feedback factor, in this case 100 times. If the values of R_T applicable to points B and C are divided by 100, the points B′ and C′ are obtained, which lie on

curve (*a*). In short, the curve DCB is a replica, on an enlarged scale, of the initial part DC′B′ of curve (*a*).

(7) CONCLUSIONS

By the use of a feedback-amplifier technique, the voltage induced in a search coil surrounding a ferromagnetic core can be made to follow closely a driving voltage of any required waveform. Harmonic generation in the $\dot{B}$ waveform is reduced by a factor approximating to the feedback-amplifier loop gain.

The method has been applied to obtain sinusoidal flux waveform in iron-testing equipment. This avoids errors which arise in the iron itself from harmonic eddy-current losses. In circuits in which wattmeters are replaced by measurements of voltages and impedances at fundamental frequency, of which a.c. bridges and the H-coil type of single-strip tester are examples, a further source of error, arising from harmonic losses in various parts of the circuit, is avoided. Ring samples of non-oriented transformer steel have been excited to flux densities of $2\,Wb/m^2$ with less than 1% harmonic distortion, and bridge measurements are quoted for $1 \cdot 7\,Wb/m^2$.

Further advantages of the amplifier method arise from the high degree of amplitude- and frequency-stability attainable. These factors contribute to speed and accuracy of measurement. By including a resistance network in which one component is made proportional to the mass of the test sample, automatic B-setting is achieved without readjustment of the driving voltage.

The technique has been extended, using three amplifiers, to obtain substantially uniform field conditions along the length of a single-strip specimen.

In the Appendices, considerations on the design of suitable amplifiers are outlined, and oscillator requirements are stated. Reference is also made to an audio distortion indicator.

(8) ACKNOWLEDGMENTS

The authors wish to acknowledge their indebtedness to Mr. H. K. Moneypenny, who originally proposed the use of feedback technique for flux waveform control; to Messrs. J. K. Darby and P. Milne, who made contributions to various aspects of the work described; to Messrs. Joseph Sankey and Sons Ltd., for encouragement in developing iron test equipment; and to the directors of Guest, Keen and Nettlefolds Group Services, Ltd., for permission to publish the paper.

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(10) APPENDICES

(10.1) Amplifier Design

When the output power requirements and the degree of feedback have been determined, as outlined in Section 3, the general arrangement of the amplifier can readily be determined by conventional methods. Referring to Fig. 2, if it is assumed that an oscillator drive of 2 volts is available, the feedback winding on the test sample will be designed to provide 2 volts at the highest test flux density, corresponding to maximum power output. A 27 VA amplifier employing a push-pull triode output stage will require 70 volts between its input grids. To provide a loop gain factor of 48 (as required in Section 3.1) the voltage amplifying stages should provide an amplification of $48 \times 70/2 = 1\,680$, which may conveniently be obtained by a pentode input stage with an amplification of 100, followed by a triode stage with an amplification of about 18.

Stabilization of the amplifiers against oscillation follows normal procedure, e.g. as outlined by Macrae.[14] It is found convenient to use push-pull stages throughout, because this provides flexibility in applying input and feedback signals to separate grids, and enables low-frequency stabilizing networks to be included in long-tailed cathode circuits. A typical amplifying stage is shown in Fig. 9, in which C_2 forms a high-frequency correcting network in association with the anode circuit resistors, and C_3 forms a low-frequency correcting network in association with the cathode resistors. Inter-stage coupling time-constants such as R_1C_1 are made large compared with the two L/R time-constants associated with the shunt inductances of the output transformer and the test coil, to ease the stabilization problem.

In the design of a larger amplifier of 1 kVA rating, intended for wattmeter tests on various types of core and also for a.c. bridge

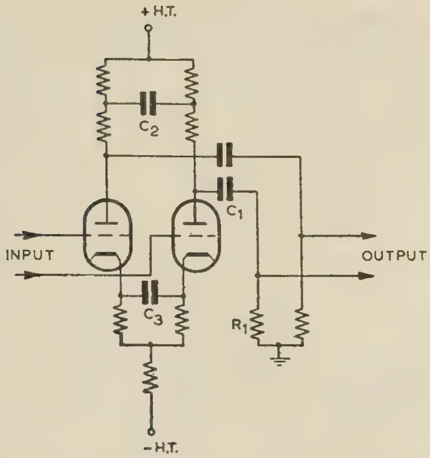

Fig. 9.—Typical voltage amplifying stage.

testing, the frequency response of the test core and its associated circuit is subject to variation over wide limits. The feedback loop must remain stable in spite of these variations. The problem is accentuated by the variation of shunt inductance of the output transformer, resulting from the change in permeability which occurs at different excitation levels. The effects of both these sources of variation cause difficulty in the stabilization of the amplifier. This has been overcome by a subsidiary feedback, shown in Fig. 10, which is applied from the amplifier output

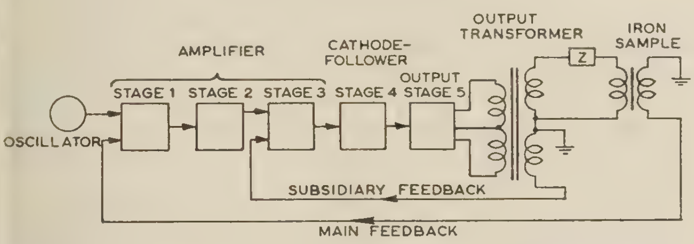

Fig. 10.—Arrangement of 1 kVA amplifier with two feedback loops.

transformer to the grid of the last amplifying valve. Its effect is to widen the bandwidth of the output stage in the manner shown in Fig. 11(*a*). At the low-frequency end of the response curve, the output transformer inductance variations cause the 3 dB attenuation point to lie between the limits shown by A and B, without feedback. With subsidiary feedback, the corresponding points are C and D, and are equivalent to an increase in transformer inductance by a factor of 10. The output stages enclosed by this feedback loop are now considered as a component of the main amplifier, having a frequency response within the limits defined by curves C and D, enabling the main feedback loop to be more readily stabilized. Since the amplification of stage 3 (Fig. 10) is effectively lost in providing the subsidiary feedback, an additional voltage amplifying stage is required. The main feedback loop is then stabilized in the usual way by appropriate networks in the first two stages. Similar networks in the third stage are employed to stabilize the subsidiary loop. The main loop response is shown in Fig. 11(*b*).

It is pointed out in Section 3.1 that the higher impedance of pentodes compared with triodes necessitates a higher feedback factor, and satisfactory stabilization becomes more difficult to achieve. In designing amplifiers of 60 and 150 VA rating employing pentode output valves, it has been found convenient to employ a subsidiary feedback loop. This may be considered

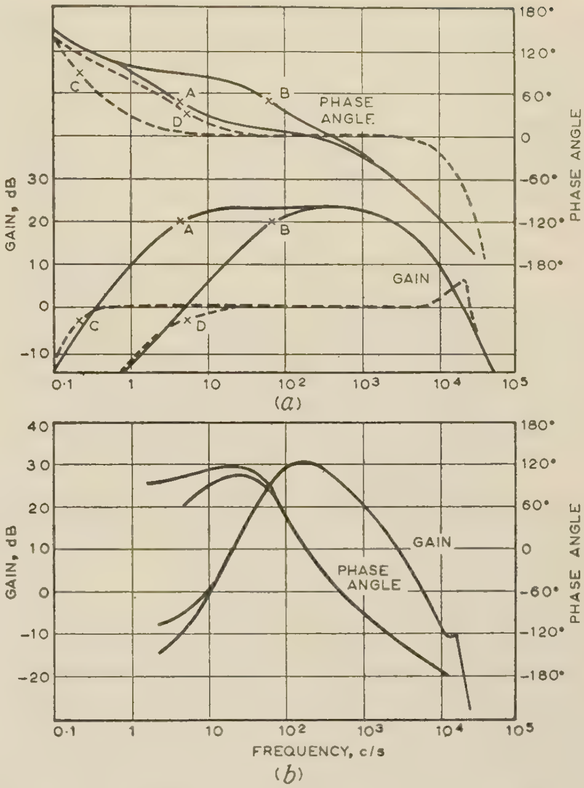

Fig. 11.—Frequency-response curves for 1 kVA amplifier.

(*a*) Subsidiary loop.
——— Open-loop response.
— — — Closed-loop response.
(*b*) Main loop.

to reduce the output impedance of the pentode (by a factor of 10 if 20 dB of feedback is used) so that considerably less distortion is produced in the iron, and the feedback requirement in the main loop is correspondingly reduced.

(10.2) Oscillator Requirements

When applied to iron testing, it is desirable that the oscillator should have frequency and amplitude stability of the order of 0·1%, and low harmonic content. When the oscillator is used at 50 c/s, the mains frequency may differ slightly and a beat-frequency component may appear in the output. It is therefore important that the oscillator should have a low mains-hum content.

The Wien bridge oscillator satisfies all these requirements, and is discussed elsewhere.[15]

(10.3) Audio Distortion Detector

When a feedback amplifier is driven beyond its maximum signal-handling capacity, there is a sudden transition from

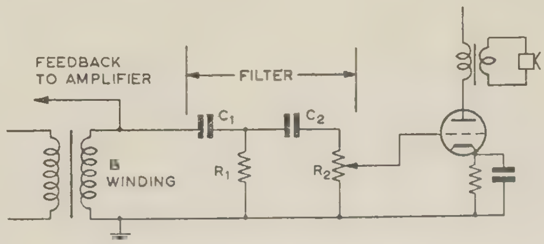

Fig. 12.—Audio distortion detector.

undistorted to heavily distorted output. It is important to the accuracy of the applications described in the paper that testing should not be continued if overloading occurs. A simple audio monitor, shown in Fig. 12, has been found particularly suitable as a warning device. The voltage appearing across the B-coil is applied to a filter which attenuates the fundamental frequency while accepting harmonics. In normal operation, no harmonics are present and the loudspeaker which follows the filter remains silent. If the power amplifier is overloaded, harmonics appear across the B-coil, and are transmitted through the loudspeaker, giving audible warning.

A simple form of filter is shown. The time-constants R_1C_1 and R_2C_2 are chosen to produce a bass cut from 500 c/s. Thus a 50 or 60 c/s test frequency is attenuated about 40 dB, which suffices, in combination with the low bass-sensitivity of a small loudspeaker, to silence the 50 or 60 c/s note.

DISCUSSION ON THE ABOVE THREE PAPERS BEFORE THE MEASUREMENT AND CONTROL SECTION, 11TH MARCH, 1958

Mr. C. E. Webb: The application of the feedback amplifier equipment described by Messrs. McFarlane and Harris to a.c. bridge tests is of particular interest, since one of the most serious limitations to the use of such methods is the distortion normally produced in bridge circuits. Many years ago Mr. Ford and I* showed that bridge and wattmeter tests gave consistent loss results when the power associated with harmonic components of current and voltage was allowed for: Fig. 8 of this paper gives direct proof of the identity of the values obtained by the two methods when ideal conditions are realized.

The paper by Messrs. Blomberg and Karttunen describes a large volume of careful work, but in the absence of empirical confirmation of the accuracy obtained there are one or two points which raise doubts. The main difficulty in measurements of this kind is usually the correct assessment of H. In Section 3.2.1 the authors estimate that their arrangement of H-coils involves an error of about 0·5%. The derivation of this figure is not clear, and in view of the well-known dependence of a.c. potentiometers—and their H-coil is essentially an a.c. potentiometer—on precise location of the ends and uniformity of winding, a larger error might well be expected. I am also puzzled by the statement at the end of Section 4.1 that 'the magnetizing curves at 50 and 200 c/s agree perfectly within the limits of accuracy of reading of the measuring instruments'. Since they find, in agreement with Edmundson and Brailsford, a considerably lower permeability with 50 c/s than with d.c. magnetization, presumably due to eddy currents, a further considerable reduction in permeability with 200 c/s magnetization would be expected.

In the paper by Messrs. McFarlane, Milne and Darby the comparative results in Table 1 are of considerable interest. The agreement between single-strip and Lloyd–Fisher results is satisfactory, but it is difficult to account for the 1–2% discrepancy between Lloyd–Fisher and Epstein squares. It appears to be common experience that the Epstein square gives lower loss values than the Lloyd–Fisher—I have found discrepancies of 3%—and the authors say that this is 'presumably due to corner-joint effects'. I have tried to find such an effect which would account for the lower Epstein values, but the departures from the assumed ideal conditions which occur seem, in general, to favour the opposite tendency. For example, longitudinal non-uniformity of B due to the reluctance of the corner joints is known to produce a reduction in the apparent loss per kilogramme, but such non-uniformity is normally less in the Epstein square than in the Lloyd–Fisher square. Eddy-currents due to the normal component of flux in the overlapped parts of the circuit provide a possible explanation. An attempt I made to obtain evidence for this effect by separating the losses in comparable Lloyd–Fisher and Epstein tests was inconclusive, since the limits of error in the separation were of the same order as the differences to be detected. I should be interested to know whether the authors have any evidence bearing on the point, which is of practical importance in connection with the standardization of apparatus for loss measurements.

Mr. D. Edmundson: The papers by Mr. McFarlane and his colleagues are particularly welcome in putting on record work which has been carried out over the last few years with conspicuous success, and which has already attracted grateful imitators. I believe their whole-sheet tester to be the most satisfactory ever developed for this purpose; but it is an interesting exercise to count from Fig. 9 of Paper No. 2553 how many sheets passed by the tester could yet have been rejected on subsequent testing by orthodox means.

It seems likely that this development has become established at a moment when it is likely to pass into history. Whole-sheet testing is economically justifiable only for the low-loss transformer grades, which constitute perhaps 30% of the total output of magnetic steel. Within two years from now, or perhaps earlier, nearly all transformers in Britain will be made from grain-oriented steel, which is made in the form of continuous strip. Now this tester will not handle continuous strip; but even if it could, or if it were used on that proportion of the output sold as sheet after cutting from the coil, it would still be ineffective, for this material is produced in such a way that its final properties are not developed until it has received a strain-relief anneal, which is not given until it has been cut into transformer laminations.

The feedback supply oscillator described in the paper by Messrs McFarlane and Harris, however, has a real future, and is in a sense already classic. The authors are ingenuous in comparing it with a 1 kVA alternator, for it is easy and economic to make alternators with much larger outputs, or very low impedance. This method of supply would have been ideal for the tester described in the paper by Messrs. Blomberg and Karttunen, for they seem to fall into the difficulty of having produced a measuring system which would have been excellent if their waveform had been pure, but a tester whose leakage flux is such that a pure waveform is impossible without a feedback-amplifier supply. I should like to know more about the performance of their H-coil, which is a disjointed magnetic potentiometer and looks dangerously susceptible to radial flux components, which must be characteristic of their tester.

Mr. L. W. Law: My remarks concern the paper by Messrs. McFarlane, Milne and Darby and have particular reference to the single-sheet testers. The authors refer to the commercial application of two such machines, for testing 8 ft and 6 ft sheets respectively. These have been in operation for some time, the 8 ft one for 11 500, and the 6 ft one for 7 500, operating hours. About six sheets a minute are tested, and there are three operators. There are frequent checks of the accuracy of the behaviour of the plant, and checks on the oscillator frequency have never been found to be more than 0·3% out. There are built-in calibration checks which are used daily, and every hour a known sheet is put through the tester to check any drift which may have taken place during that time. The overall calibration of the machine is done in the following way. For each batch of

* Webb, C. E., and Ford, L. H.: 'Alternating-Current Permeability and the Bridge Method of Magnetic Testing', *Journal I.E.E.*, 1935, 76, p. 185.

material which is graded a sheet is taken and its actual power loss is measured. A test piece from that sheet is cut to Lloyd–Fisher size, half with and half across the grain, and is subjected to the Lloyd–Fisher test. These results are analysed and at intervals, if necessary, the calibration of the machine is altered. This takes into account all the errors and is based on the principle that only sheets showing the specified loss for the grade or less are passed for that grade.

Much use has been made of this machine in experimental processing of sheets. The loss of a sheet can be measured, the sheet can be subjected to further processing and then remeasured, thus directly showing the effect of the process.

Mr. A. C. Lynch: I refer to that part of the paper by Messrs. McFarlane and Harris which deals with bridge methods.

Fig. 8 suggests that bridges are less satisfactory than wattmeters; but is it really necessary to use R_T/Z ratios as high as those shown? Might this graph, while giving a vivid impression of the error which could arise in an unfavourable case, exaggerate what would occur in a typical measurement? By using widely differing values of Z_B and Z_C—such as 1 ohm and 100 kilohms—we have reached a value of R_T/Z of 0·3 at 15 kG. These extreme values of Z_B and Z_C might be expected to lead to low sensitivity, but they do not.

There are two possible reasons for preferring bridges to wattmeters. First, since the quantity measured by a wattmeter varies approximately as the square of the flux density, while that measured by a bridge is nearly independent of it, the errors in calibrating and reading the instruments can accumulate in a wattmeter method but can scarcely affect a bridge. Second, bridge methods are more suitable for dealing with the very small cores, weighing only an ounce or so, which are convenient for use in laboratory experiments with new materials or heat treatments.

Mr. E. Rawlinson: I am pleased to see the papers by McFarlane *et al.*, because we were fortunate enough a few years ago to receive a good deal of information from the authors, and we built an equipment similar to that described, which proved very satisfactory. We can fully support the claim of about 1% for the square testers, in particular for the Epstein square, which we have mostly used. I think that the general point can be made that electronic methods have been introduced into precision iron-loss measurements to such an extent that the time taken in measurement is now purely a question of weighing the steel and assembling it into the tester.

We have checked the actual instrument error in the wattmeter against a standard electrodynamic wattmeter and have so far failed to find more than ±0·1% error at full scale, even at low power-factors. We use wattmeters of the portable type with rather more delicate suspensions and have maximum deflections for power factors of 0·05 or less. With such an instrument we can measure up to quite high flux densities, in so far as the wattmeter will still be accurate, and for a power factor of 0·05 we get full-scale deflection for about 15·5 kG for 4% silicon hot-rolled steel and 19 kG for cold-reduced oriented steel.

In the whole-sheet tester it would be interesting to know what uniformity of flux density is obtained in the sheets, and how many parallel coils are used for this. It is also stated that a 1 kVA amplifier is necessary with feedback to produce a sine wave. How much distortion is actually present there, and can the whole-sheet tester be used for flux densities exceeding 13 kG?

With the single-strip tester, where a uniform flux is established and where the measurement is made with the aid of *B*- and *H*-coils, is not the answer the true specific loss of the material?

Dr. O. I. Butler: At Sheffield, Dr. J. N. Fletcher and I have developed an alternative method to that described by Messrs. McFarlane and Harris for controlling the flux waveform in iron testing by means of a feedback amplifier. The main advantage of our method is that the amplifier is required to supply only the harmonic apparent power, the fundamental excitation being supplied directly from the 50 c/s mains. Thus the output of the feedback amplifier is injected into the excitation circuit, in series with the 50 c/s mains, via an acceptor circuit tuned to 50 c/s. As in the case of the authors' method, any waveform of excitation can be impressed upon the specimen. In particular, for sinusoidal magnetizing forces rather than sinusoidal flux conditions of the specimen, the total supply apparent power is required to be of fundamental frequency only. Hence, the amplifier used in our method is not required ideally to supply any apparent-power output in this particular case; its function is merely to provide harmonic voltages to balance those generated by the specimen and thereby suppress the circulation of harmonic current. Our interest in this condition is a practical one, since it more closely approaches the high magnetization conditions actually encountered in many rotating a.c. and d.c. machines.

I presume that the authors will agree that their use of three amplifiers in parallel, to obtain uniform loading along the length of the specimen, is inadmissible for any waveform other than sinusoidal flux, since it is virtually impossible to use an *RC* circuit to compensate for the relative phase shifts of the amplifiers in respect of the harmonic, as well as the fundamental, flux.

Messrs. McFarlane, Milne and Darby claim that the 'parallel connection of the sections of the magnetizing coil, combined with the low reluctance of the yoke, provide an adequate degree of flux uniformity along the length of the specimen'. It would be helpful if a quantitative statement could be made on this point.

Mr. P. H. King: I was interested in the mention of phase shift between the three amplifiers driving a single-strip tester, referred to in Section 5 of the paper by Messrs. McFarlane and Harris, having had experience of a very similar arrangement. I should like to know how this phase angle was discovered and measured, since it will not be obvious that an error exists in an apparatus based on first principles. Fig. 6 illustrates a useful aspect of compensating amplifiers, where, instead of several coils contributing increasing amounts of compensation to the loss outside the length of sample being measured, the separately driven coils take on the whole job. All the other coils then take equal current, which means that the parallel connection of many coils is no longer necessary and a single coil would function equally well. Using a central coil about 10 cm long between compensating coils each 5 cm long, we have experienced no trouble from phase shift, and tests with search coils have demonstrated uniformity of magnetization within $\pm\frac{1}{2}\%$ along the central measurement section. This is probably due, as the authors suggest, to the fact that our three amplifiers have much more nearly similar loads.

In Section 7 of the paper by Messrs. McFarlane, Milne and Darby, where the calibration of these equipments is described, no mention is made of any built-in frequency check. Any change in frequency would immediately lead to errors in calibration, and it would seem advisable to include in such apparatus a frequency standard such as a crystal oscillator.

Rectifier meters are often used in the circuits described. The normal meter rectifier tends to be too temperature-sensitive to provide the 0·1% accuracy aimed at in individual measurements, and I wonder whether any special precautions were taken to compensate errors arising from variations in the ratio between forward and reverse impedance. Wherever a peak voltage is measured by the average of its differential (as in the B_{max} and H_{max} circuits), this ratio must be maintained at a high value. Thermionic rectifiers or silicon diodes would seem the best choice.

Mr. G. W. Eastwood: I hope to publish shortly a paper

describing a bridge for measuring iron loss which is very similar to that described by Messrs. McFarlane and Harris. Once the specimen flux waveform has been made sinusoidal, the loss may be measured by any kind of bridge, but the most suitable are those such as the Hay bridge used to measure the equivalent shunt components of the mutual admittance between two equal windings on the specimen. The power loss is then V^2G, where V is the r.m.s. voltage—which can be measured with any kind of meter, since no form factor problem arises—and G is the measured shunt conductance. The authors used a Maxwell bridge, which measures series components, so that a more complicated calculation is required. Why did they do this?

Bridge methods have the advantage that they depend only on balancing voltages in passive networks which can be made from precision components; such networks work equally well at high or low levels and so for large or small specimens. The subsequent amplifier must be selective, since the bridge is not balanced to the harmonics, but its gain need not be known exactly. This is in contrast to the use of amplifiers to drive a wattmeter when small specimens are measured, where the amplifier gain and phase shift must be critically controlled over a wide frequency band. I have found a selective amplifier using parallel-T feedback circuits to be satisfactory, and I should like to know what kind of detector the authors used.

With a commercial power amplifier about 20 dB of feedback can be obtained without special shaping of the feedback loop, and this is sufficient for measurements at inductions up to 15 kG in silicon-iron and 6 kG in high-permeability nickel-iron.

Dr. D. Hughes: In Section 4.2 Messrs. McFarlane and Harris refer to the *dB/dt* waveform as containing less than 1% third-harmonic distortion. Have they related this percentage distortion to the variation in form factor from that of a sine wave? I believe this to be an important point, for the form factor is present as a squared term in the equation for the loss when one specifies as a parameter the peak value of the induction. It is, however, somewhat difficult to arrive at an accurate value for the form factor of a given waveform. If one measures upper harmonics with a waveform analyser and endeavours to infer the form factor, the calculation becomes intractable: if, on the other hand, one endeavours to measure the form factor directly, one is at once presented with the problem of producing an accurate mean-responding voltmeter. This is a point in which I have been interested for some time. Could the authors elaborate on it?

THE AUTHORS' REPLIES TO THE ABOVE DISCUSSION

Professor H. Blomberg and **Mr. P. J. Karttunen** (*in reply*): First, we wish to thank Mr. Webb and Mr. Edmundson for the kind esteem they have shown our work, and since their comments and questions tend to be related, we shall comment on them together. In their remarks relating to the H-coils both stress the weak point of our tester. The H-coils are one of the details mentioned in the Introduction which have not been sufficiently studied, since our primary aim was to produce a working entity. Attempts were made to restrict the measuring region to that part of the test specimen where the radial field would be as low as possible. The constants for each component coil were determined by compensation measurement in a homogeneous magnetic field. The error of 0·5% mentioned in Section 3.2.1 only takes into account the circumstance that the axes of the radial component coils 1 and 6 cannot be placed immediately to adjoin the ends of the axial coil. This value was calculated from curves showing the variation of the axial and radial component of magnetic field strength measured in the first stage of the design work. Theoretically, the radial field in the measuring section can be made arbitrarily small by increasing the length of the test specimen and the magnetizing coil. However, there are other critical circumstances connected with the H-coils, such as the finite cross-section of the coils, the spaces between component coils 2, 3, 4 and 5, the small space between the ends of the test specimen and of component coils 1 and 6, and the influence of the inductance and capacitance of the coils, although the calculated angular error these cause amounts only to about 0·01′. It is interesting to note that, although a fairly strong radial field exists in the vicinity of the test specimen, its effect in the measurement of the iron losses is comparatively negligible. We have performed an experiment in which, at one and the same magnetization, the readings of the H_{max} and P_{Fe} instruments were recorded as a function of the distance between the test specimen and the H-coils. The increase of the H_{max} reading with increasing distance demonstrated the presence of a fairly strong radial field, but the reading of the P_{Fe} instrument remained practically constant. This shows that there are phase differences in the field in the vicinity of the test specimen. We regret the observation made at the end of Section 4.1 to the effect that 'the magnetizing curves at 50 and 200 c/s agree perfectly within the limits of accuracy of reading of the measuring instruments', since this is not strictly true. Checking this circumstance, we found that in the measurements on which this observation was based the difference between the curves obtained at 50 and 200 c/s was small, indeed, but this is only a particular case and it is due to the fact (as could be ascertained by purely theoretical calculations as well) that in these sheets the effect of the eddy currents is small. However, the difference between the d.c. and a.c. curves shown in Fig. 14 cannot result from eddy currents alone, since, calculating from the permeability (0·006 8) corresponding with the plot for $H = 1 \cdot 1$ amp/cm and from the conductivity of the sheet in question ($2 \cdot 7 \times 10^4$ mho/cm), the average decrease of density at 50 c/s caused by the eddy currents in a 0·5 mm sheet of infinite extension, the result is found to be only about 1%. In the Figure the difference between the d.c. and a.c. curves at $H = 1 \cdot 1$ amp/cm is about 20%.

Messrs. J. McFarlane, P. Milne, J. K. Darby and M. J. Harris (*in reply*): Mr. Webb and Mr. Rawlinson have commented on the Lloyd–Fisher and Epstein testers. We note with interest that Mr. Webb has also found that Epstein tests produce lower loss figures than the Lloyd–Fisher ones, but we are unable to offer further explanation of the reasons for this. Mr. Rawlinson's application of low-power-factor wattmeters has usefully extended the upper limit of test inductions.

Various questions have been asked regarding single-sheet and strip testers. In reply to Mr. Rawlinson's questions on the sheet tester, each magnetizing coil is 3 in long, requiring 28 parallel coils over a 7 ft test length. At 13 kG the third-harmonic content is 1%. The tester was designed to operate at 13 kG, and the amplifier overloads at about 14·5 kG. The following Table shows the degree of flux uniformity achieved in the single-

Test length measured from pole tip	Relative flux density	
	Sheet tester	Strip tester
%	%	%
3	98·7	98·2
10	99·0	99·5
20	99·8	99·9
50	100	100

sheet tester, and also—in answer to Dr. Butler—in the low-loss yoke type of strip tester which employs seven parallel coils.

In applying the 3-amplifier method to strip testers we agree with Mr. King that a single central coil is permissible, and we note with interest his success in its application. Phase-shift errors were deduced from measurements of the open-loop gain and phase shift of each of the three amplifiers. Their effect on loss measurements was measured by interposing a known phase-shift circuit between the oscillator and the amplifier. We agree with Dr. Butler that, so long as phase shift is present, the 3-amplifier method can be used only for sinusoidal waveforms. With nearly similar loads, as used by Mr. King, phase shift may no longer be troublsome.

The magnetization system which Dr. Butler describes has the merit of requiring a somewhat smaller amplifier, but its usefulness is limited by the voltage and frequency stability of the mains supply.

Mr. King also mentions the need for checking oscillator frequency. We have used the amplifier to drive a synchronous-clock motor, which is compared with a stop-watch over a period of several minutes. As a further safeguard, the Wien-bridge circuits which determine oscillator frequency are chosen to have zero temperature coefficient. Temperature effects on instrument rectifiers have been overcome, as Mr. King suggests, by employing silicon diodes, which are preferred to thermionic diodes.

Dr. Hughes points out that the calculation of form factor from waveform-analyser measurements is intractable. For a given set of circuit conditions, the relationship could be measured empirically, but we have not attempted to do this. Fig. 4 of Paper No. 2554 suggests that, for a particular circuit, a loss error of 1% will arise (at $1 \cdot 27\,\mathrm{Wb/m^2}$) when the third-harmonic content is 8%. A lower value is desirable to reduce errors still further, and it is probable that a value of 2% or even 5% would be permissible in many cases. Our choice of 1% of third harmonic is considered to be a very safe target, and it has proved to be readily attainable.

Messrs. Lynch and Eastwood have commented on the advantages of bridge measurements over wattmeter methods. At present we prefer the direct-reading wattmeter methods for large-scale testing at power frequencies, because of the shorter testing time. The bridge is used at higher frequencies, and also for tests on samples of low mass.

In reply to Mr. Lynch, the values of R_T/Z in Fig. 8 of Paper No. 2554 were higher than might be obtained in a typical case, partly because the arm Z_A also included a wattmeter, and partly because the impedance Z had to be low to match the output impedance ($2\frac{1}{2}$ ohms) of the amplifier available. We agree with Mr. Eastwood that the Hay bridge is normally preferable to the Maxwell bridge, but in the case quoted we required the effective series resistance of the bridge arm before subtracting the wattmeter coil resistance. Our bridge detector, like Mr. Eastwood's, employs a parallel-T feedback network.

DISCUSSION ON 'CHOICE OF INSULATION AND SURGE PROTECTION OF OVERHEAD TRANSMISSION LINES OF 33 kV AND ABOVE'*

Before the Irish Branch *at* Dublin *21st February, the* North-Western Supply Group *at* Manchester *26th February, the* North Midland Centre *at* Leeds *29th October, and the* Northern Ireland Centre *at* Belfast *12th November*, 1957.

Mr. W. P. Leech (*at Dublin*): A distance of about one mile is quoted as the length to which earth wires should extend from a station, but I think that this statement might be qualified in some way depending on whether rod gaps or surge diverters constitute the station protection. With rod-gap protection I think that a length of even more than a mile of earth wire might be required so that steep incoming waves might be sufficiently attenuated to allow the rod gap to give reasonable protection. With surge diverters, on the other hand, what is required, I think, is a sufficiently long earth-wire section to ensure that the surge impedance of the line might be effective in reducing the current amplitude and the rate of rise of current, and so limit the voltage. In this case a shorter distance, possibly half a mile, might suffice for most waveshapes experienced in practice.

The use of unearthed metal cross-arms is mentioned as a possible means of improving line performance, but while this might be valid for systems employing a solidly-earthed neutral, it would, I think, require to be qualified for systems earthed through arc-suppression coils. The use of unearthed metal cross-arms on such a system might merely result in a reduction in the number of single-phase earth faults, which would of themselves have been relatively innocuous in any case, and an increase in the number of 2-phase or 3-phase faults, with consequent circuit-breaker operations and possible interruption of supply.

Dr. R. C. Cuffe (*at Dublin*): The authors state, first, that only strokes to ground are of importance in overhead-line transmission, and secondly, that indirect lightning strokes are now known to be of no practical importance on systems operating at about 33 kV and above. It is rather hard to accept *in toto* these two statements. Do the authors contend that the phenomenon described as 'release of bound charge' on a conductor resulting after a cloud-to-cloud discharge could not initiate a surge of sufficient magnitude, say, on a tail-fed 33 kV transformer feeder to cause trouble at the transformer if the latter were within a mile or so of the disturbance?

Again, it is felt that induced surges are at least somewhat responsible for the known higher fault rates of lower-voltage systems such as 33 kV when compared with those of 132 kV systems. Do the authors attribute the substantial differences in the fault rates solely to the greater ability of the 132 kV system to 'digest' more direct strokes?

From an analysis of fault statistics in Ireland it would appear that, while published meteorological data shows a variation of about 3 or 4 to 1 in the isoceraunic levels of various parts of the country, the lightning fault rates between various areas (as derived from statistics on lines of similar voltage design and con-

* Thomas, A. Morris, and Oakeshott, D. F.: Paper No. 2158 S, August, 1956 (see [illegible] A, p. 229).

struction covering some 20 years' operation) show well-established variations of perhaps 15 to 1 or more. Can the authors advance any explanation for this divergence?

Mr. P. P. Bonar (*at Dublin*): Methods adopted to provide against the effect of atmospheric pollution of line insulators do not appear to follow any set pattern. We know of many different practices for combating the deterioration of insulation due to salt deposits or industrial pollution, e.g. the application of waxes or greases of the silicone type to the insulators, and the development of special forms of insulator sheds designed to give a longer overall creepage path or extra shielding to the underside of the sheds. The anti-fog types of insulators used in Great Britain are of such a form as to combine longer leakage path with fuller shielding of the underside of the shed, but I am sceptical about the value of this extra shielding as protection against contamination, particularly in coastal districts and in the more severely polluted atmospheres of industrial areas. Severe contamination of the shielded portions of insulators does occur, and I have seen insulators returned from service with their undersides completely encrusted. The additional shielding would seem to present little protection against contamination and at the same time to prevent any beneficial washing effect of driving rain. The value of 1 in per kilovolt between phases is quoted by the authors as the minimum creepage path for insulation in polluted areas, but even this is considered inadequate for the highest system voltages and a greater value is suggested in such cases. Perhaps the authors would say whether the longer creepage path recommended is, in actual fact, a minimum requirement for the higher-voltage systems, or is it recommended merely to provide a higher factor of safety?

In regard to wood-pole systems, it is accepted that an increase in insulation of approximately 50 kV per foot of wood can be taken when considering insulation strength against impulse surges, but from the point of view of power-frequency surges what insulation value, if any, can be taken for wood?

Mr. A. Burke (*at Dublin*): Monumental work has undoubtedly been done on lightning, but the end is not yet in sight. Extensive and laborious observations and measurements have been made and conclusions have been drawn. While these reasonably fit the particular circumstances, they may be only partly correct and hence inapplicable for general theory. Some factors, such as isoceraunic level, are so unsure that conclusions on associated problems may well be invalid.

In particular, the present lack of knowledge of the properties and mechanisms of the lightning channel makes highly problematical all theories on the detailed merits of variations in insulation, clearance, tower height and earthing at and near the point struck. It is not clear to what extent the properties of the channel are affected by those of the point struck, and vice versa.

Likewise there appears to be little exact knowledge on induced strokes. They are dismissed as being of no importance on systems higher than 33 kV, that is, they do not exceed about 300 kV. It seems, however, that with, say, six 110 kV lines entering a station, a stroke in the vicinity of the station may induce 300 kV in each line, thus giving 1 800 kV on the main busbars. Further, on passing through the transformers, the three phases would add up on the neutral bar, thus giving on the arc-suppression plant a voltage corresponding to 18 times the initial surge of 300 kV. Tertiary windings are likewise affected, but inversely as the number of transformers in service. I should value the authors' comments on this point.

Mr. F. S. Edwards (*at Manchester*): Calculations are given in Section 7 of the estimated performance of lines when subjected to lightning strokes. These calculations are based on the assumed isoceraunic level, which itself is a very uncertain quantity and varies from year to year and from place to place even over a small area.

The authors say that this level is not an accurate measure of the frequency of lightning strokes to earth, and indeed the selection of round numbers for α in eqn. (4) indicates the essential lack of precision of the calculations. I should say that I have not overlooked the remark that high absolute accuracy cannot be expected.

The only effective check on the accuracy of the predicted performance of a line is the actual performance, and it is on this point that I have looked in vain for information in the paper. In the absence of reasonable correlation between theory and practice the calculations are only mathematical exercises and are of no use as a guide to the design of line insulation.

If shortening necessitated the deletion of data on actual performance, or if the authors have accumulated records of faults since the paper was written which confirm their predictions, I hope that they will be able to include some of them in their reply, as I am sure that such data will be of great interest and value.

Mr. F. Mather (*at Manchester*): The voltage developed across the inductance of a steel tower or a down-lead to earth on a wood-pole line, when the steep front of a surge is passing, may be extremely high, say 100 kV, with a current of 20 kA and a wavefront of 1 microsec. This voltage will not coincide with the drop across the footing resistance, so the two values will not be additive. Can the authors confirm that there is no point in spending money on a lower footing resistance than is necessary to ensure that the peak value of Ri does not exceed that of di/dt?

Fig. 2 shows that in 20% of strokes the current exceeds 40 kA, so that to obtain 80% protection it appears necessary to cater for currents of 20 kA flowing each way when a line is struck. Currents of 20 kA produce such high voltages in diverters and their connections that the degree of protection appears doubtful.

The authors refer to an average waveshape of 5–25 microsec, whereas a recent foreign summary indicates that in 50% of surges the current front is below 2 microsec. This value would appear to have a considerable bearing on the risk of backflashover. It is surprising to note from Section 4 that the surge resistance of a rod or plate electrode is equal to, or lower than, the low-frequency resistance, whereas for a strip electrode it may be about 150 times as great.

Section 7 shows that on overhead lines without earth wires and with an impulse level below 1 000 kV, all strokes exceeding a few hundred amperes will cause flashover to an adjacent tower or phase conductor. Since practically all strokes will exceed this value, does it not follow that, on 33 kV wood-pole lines, flashover will almost invariably occur and cause the line protection to operate irrespective of the provision of diverters at the ends of the line?

Do the authors advocate the use of high-speed reclosing switchgear on 33 kV as well as lower-voltage systems in preference to attempts to afford protection by increasing the insulation level or use of diverters? Is any information available as to the effect of rapid reclosing on motors?

Mr. W. H. Thompson (*at Manchester*): Reference is made to the I.E.C. relationship between system voltage and power-frequency and impulse withstand voltages. It is then stated that a different method of approach will be required for lightning. Surely the I.E.C. provision caters for this in that the power-frequency test proves insulation for system over-voltages, and the impulse test proves the same for impulses, including lightning up to the limit of the basic impulse level.

The statistics on lightning probability can be only very approximate. For instance, the paper quotes the velocity of the leader stroke as 200 km/sec, which is equal to 0·6 ft/microsec. An

American authority quotes the leader-stroke velocity as anywhere between 0·25 and 3·0 ft/microsec.

In Section 2.2 the effect of high footing resistance is mentioned, together with that of the junction of earth wires and neighbouring towers. This same effect applies with low footing resistance. It would be useful if the authors could give some statistics on currents through surge diverters, as these are also a junction effect on the line.

The statement on indirect strokes being of no value is extremely interesting, as it is just the opposite to what was expressed quite recently by a consulting engineer expert in this country.

In Australia it was found that two districts had lightning troubles almost in inverse proportion to the isoceraunic level. Eqn. (4) becomes of reduced value as isoceraunic level becomes unreliable.

In Section 4 it is stated the resistance of an earth rod increases after the passage of heavy currents. The authors might have explained that this is due to drying of the soil, and the high resistance is not necessarily a permanent feature.

It might also have been explained that the impedance changes from an initial all surge impedance, when the wave first reaches the mid-point of a radial counterpoise, to all leakage resistance when the wave has reached the ends. Knowing the velocity of the wave in the counterpoise is one-third that of light, and the requirement that the reflected wave from the arm ends must add nothing to the tower voltage, it is simple to calculate the useful maximum length of arm for a given case.

On the effect of rain, the probable reduction given of 5% in impulse flashover is difficult to reconcile with the nearly 10% variation in humidity correction factor given in Table 1 for a change from 11 g/m^3 to 22 g/m^3.

On clearances, I would think the point-to-plane gap would be a better basis than the rod gap, and regarding system flashovers, can the authors give some information on the C.E.A. experience of flashovers on their 275 kV system?

Mr. W. J. A. Painter (*at Leeds*): I would have expected the authors to have quoted briefly figures relating to lightning faults to emphasize the importance of the subject of the paper. Many of these are given in Reference 18, so I will only mention now that, for the period 1933–47, overhead lines were involved in 94% of all lightning faults on the Grid system in this country.

Section 7 is an interesting approach to the problem, which gives solutions in line with practice in the cases considered. In his earlier paper, Dr. Forrest showed that up to 132 kV the lightning fault rate was approximately inversely proportional to system voltage and led to the view that the fault rate would continue to fall at higher voltages. Yet in fact we have found, admittedly over a shorter total length of line and a shorter period of time, that the fault rate on 275 kV lines is appreciably higher than that on 132 kV lines. Have the authors estimated the performance of 275 kV lines on the basis of Section 7, and are their conclusions in line with practice and experience? I suggest they will not be so and therefore feel that some factor is missing in the authors' approach. Might this factor be tower height or tower inductance? Possibly the reason is that at 275 kV we are approaching the optimum figure of 300 kV quoted in Section 6. Perhaps the authors can say what margin should be considered on either side of 300 kV in deciding upon the basis for determining the line insulation.

The curves for tower-footing resistance start at values of 10 ohms in Fig. 7, and 30 and 20 ohms in Fig. 8. The average tower-footing resistance on 132 kV and 275 kV lines in this country is below 10 ohms. Can the authors give an enlarged section of the curves at the lower values, as these will be of more practical value in this country and would enable design engineers more clearly to assess the merit of fitting additional insulators?

There is no reference to the work by E.R.A. and others on surge diverters, and I would also have expected some comment on the effect of a cable termination.

In Section 3 one mile is quoted as the distance from the station for the installation of double earth wires. Is this still an arbitrary figure or has it been confirmed by observations or calculations of the authors?

The effect of installations for cathodic protection of pipe lines has been considered previously only in respect of neighbouring cables. We have experienced recently a case where a nearby overhead line is affected, small currents entering the nearest tower and returning to earth via the earth wire and adjacent towers. One relatively cheap and simple method which is being considered for combating this effect is to lightly insulate the earth wire from a number of towers in the vicinity of the installation. Would the authors say if they would expect such a move to have any effect on the surge protection of the lines, and if so to what extent?

Prof. G. W. Carter (*at Leeds*): Although lightning is not mentioned in the title of the paper, the discussion hardly mentions any other form of surge voltage. I should like to ask whether the authors consider that switching surges, which may involve current chopping, are not important. There is also hardly any reference to the apparatus connected at the terminals of the line, which should surely enter into any discussion of surge voltages, since it must play a great part in determining the choice of insulation and surge protection.

Fig. 2 gives information about the magnitude of current in lightning strokes, but there is no mention of the duration of the current, and it is assumed that tests in which the surge voltage decays to half-value in 50 microsec are adequate. Does available information on the duration of lightning strokes bear this out, even when a number of strokes follow in quick succession in a single flash?

It is stated in Section 4 that an earth plate has a higher electrostatic capacitance than a rod. In view of the analogy between the electric field between electrodes in a dielectric and the current flow between electrodes immersed in a conducting medium, I should have expected that electrodes giving equal resistance to earth would also have given equal capacitance to earth. I should appreciate the authors' comments on this point.

Major E. N. Cunliffe (*at Belfast*): The expression for pN derived in Section 7 for determining the lightning performance of a transmission line is simple and convenient to apply, but its value will depend upon the accuracy with which the factors p and N can be assessed. The number of strokes-to-line per 100 miles per year, N, particularly appears open to some doubt, being derived according to eqn. (4) from an estimated relationship between the isoceraunic level and the number of cloud-to-earth strokes per square mile per year. Both these factors would seem to be extremely difficult to measure with any degree of precision, as they must depend to a large extent on human observation and interpretation. Would it not be simpler and much more accurate to obtain N directly from a study of operating records relating to the various types of transmission lines in service in different parts of the world, since most large supply undertakings maintain comprehensive statistical records of the behaviour of their transmission lines in thunderstorms.

The authors are content to dismiss induced lightning surges as of no practical importance on lines operating at 33 kV and above. I would suggest, however, that this lower limit might be raised to, say, 66 kV, as I believe that experience in Eire on the Electricity Supply Board's 38 kV lines indicates that such surges can be troublesome.

Mr. W. Szwander (*at Belfast*): While the amount of insulation to be built into an overhead line is purely a matter of economics,

it must be appreciated that, depending on the part played by a particular overhead transmission line in a power system to which it belongs, the permissible frequency of outages may vary between very wide limits. For example, a line transmitting a large amount of power from a generating station, say nuclear, costing maybe 40–50 times as much as the line, ought to be built, even at considerable cost, to be virtually completely free from outages. The only practicable way of achieving this is to shield the line effectively from direct lightning strokes by provision of earth wires, while simultaneously ensuring such relationship between the tower-footing resistances, the tower structure impedance, the spacing between the earth wires and the conductors, and the insulator electrical strength that no back-flashovers will occur. This may mean that the line insulation is much in excess of the substation equipment insulation, usually corresponding to the I.E.C. standard insulation level; hence the latter will require to be protected by lightning arresters. No other means of protection (e.g. rod gaps) would be adequate on account of both the time-lag involved and of the resulting service interruptions following the flashovers. It may prove impossible, even with generous application of counterpoise wires, to reduce the tower-footing resistances to the extent of eliminating the possibility of line outage in the very few cases of lightning strokes with exceptionally heavy currents. Here only the use of more than one circuit in parallel (preferably on independent supports), together with application of high-speed auto-reclosing, can bring us near to complete elimination of the possibility of outages. In areas with very low lightning incidence, like Northern Ireland, equally satisfactory results could probably be achieved with more than one wood-pole line in parallel, though in this case also, on account of the substation equipments, the terminal lengths would still require some form of shielding.

Mr. A. A. Bromley (*at Belfast*): The curves in Fig. 8 suggest the possibility of grading the insulation of an overhead transmission line. It is conceivable that the terrain over which a projected line is planned to pass presents such diversity of soil conditions that the tower-footing resistances will vary considerably along the line. Assuming that, say, 2·5 flashovers per 100 miles per year are accepted as permissible and using the authors' curves, a line using 885 ft spans might employ from 8 to 14 discs per insulator string at individual towers, according as the tower-footing resistances varied between 30 and 80 ohms.

Such a system of grading might lead to economies where a reduction of discs per string was permissible because of low tower-footing resistances. But probably its greater value would be in those cases where high tower-footing resistances are encountered and there is little prospect of appreciable reduction by provision of counterpoises without considerable expense; the addition of discs to the insulator string might offer a more economical solution to the problem of reducing the risk of flashover on such towers.

Mr. G. F. L. Dixon (*communicated*): From the authors' reply to my contribution to an earlier discussion,* it may appear that I regard line insulation more or less as a means of providing surge protection for station apparatus. However, my point was simply this: let us have unearthed lines (where appropriate) by all means, but let us then face the fact that we shall have to install considerably more surge diverters than we did in the past.

Mr. R. R. Gilmour (*South Africa: communicated*): The formula given for the resistance, R, of a rod in soil of resistivity ρ should have been

$$R = \frac{0\cdot37\rho \log_{10}(4l/d)}{l} \text{ ohms}$$

where ρ is in ohm-cm and l and d are in centimetres.

* *Proceedings I.E.E.*, 1957, 104 A, p. 247.

This can be written

$$R = \frac{\rho}{83l}\log_{10}\left(\frac{48l}{d}\right) \text{ ohms}$$

where ρ is in ohm-cm, l in feet and d in inches, which is probably a more practical form.

It is also mentioned in the paper that the effective resistance to earth is appreciably lower for lightning-discharge currents than the d.c. or low-frequency resistance.

It is true that this is known to be so, but the reason is not obvious. In view of the high frequencies usually present it would be expected that reactance and skin effect would increase the impedance. However, it seems that local breakdown of the soil is caused by high voltage-gradients under lightning conditions.

Mr. J. H. Hagenguth (*United States: communicated*): The authors state under Section 4 that the resistance of an earth electrode is greatly increased by passage of a heavy current. I presume they mean normal-frequency currents, because it has been shown by several investigators that the resistance of an earth electrode decreases for passage of heavy surge current. It would be interesting to have a reference to this statement or some additional supporting data. Such an increase in resistance after a flashover could presumably cause a second flashover in the case of a multiple stroke, even though the amplitude of the second stroke were considerably lower, and thus interfere with successful reclosing of the line.

Fig. 4 gives the relative air density at various altitudes, based on pressure and temperature data in free space. While pressure changes on the surface of the earth are about the same as in free space, temperature changes are much less drastic. Fig. 4 shows 0° C for an altitude of 10 000 ft, whereas meteorological data on the earth on a summer day just before a thunderstorm would give much higher temperatures, of the order of 15–20° C. Therefore, the air-density factor at the earth's surface would be higher than that for free air.

The use of tower and earth-wire heights as factors is sound and should give more accurate determination of outage rates than does the American I.E.E. method, which uses isoceraunic level only as the modifying factor. However, it is now apparent that neither method can calculate the outage rate of 8·5–12·7 per 100 miles per year experienced on some American lines operating under very favourable earthing conditions with a.c. earth resistance of the order of 10 ohms and less. To explain these outages, it is necessary to modify many of the concepts such as current front, current amplitude statistics and the voltage drop existing during surge conditions on the tower earths. Some of these are discussed in a recent paper.*

Mr. K. L. Rao (*India: communicated*): I wonder if the authors can help me over a problem occurring in medium-wave broadcasting where base-insulated towers of comparatively low electrical heights are used. For operating frequencies less than about 1 Mc/s, with towers of heights varying between 250 and 400 ft, r.f. voltages arising on the towers are often quite high. With thunderclouds overhanging, frequent sparking is observed across the base insulator and across the guy insulators near the top levels of the guys. This sometimes assumes serious proportions with consequent discharge, and the transmitter gear has to be shut down or worked with very low modulation. The protective devices, such as arc-suppression in the feeder line circuits, lightning-discharge protection, etc., do not appear to be adequate.

Towers are sometimes provided with copper bonding, i.e. copper straps along the legs and concentric strips round the tower at various levels. This appears to have some effect, although not established, in reducing the trouble.

* Hagenguth, J. H., and Anderson, J. G.: 'Factors Affecting the Lightning Performance of Transmission Lines', American I.E.E. Technical Paper 57–1079.

Ocelite or similar resistors connected across the guy insulators are generally recommended.

The spark-gaps are generally connected at the base of the insulated tower, either through a discharge resistor or directly to earth. This particular earth is usually laid separately, and is distinct from that used for the radial ground system. However, more recently, I have noticed that a common earth plate is being used for both. Would the separate earth reduce discharge effects at the base of the tower, and also help to reduce voltage build-up at the base as well as along the tower?

Messrs. A. Morris Thomas and **D. F. Oakeshott** (*in reply*): The primary function of the earth-wire screen on the line adjacent to the station is to intercept lightning strokes which otherwise would strike the phase conductors. To obtain appreciable extra attenuation of incoming surges, as suggested by Mr. Leech, it would be necessary to provide an earth-wire screen for a distance of several miles. Station protection by rod gaps, strictly speaking, is appropriate only for a country with a high degree of interconnection and a relatively low isoceraunic level for which the expense of a longer earth wire as an additional safeguard would not, in general, be economic. In the paper a distance of one mile is given as representative of accepted practice, but with surge-diverter protection a shorter distance might be adequate for supplementary protection.

We agree that our conception of line performance is not, strictly, applicable to systems with arc-suppression coils. Since in such systems a single-phase fault does not normally lead to an outage, improvement in performance will involve reduction of multi-phase flashovers, and possibly earthed metal cross-arms would be of benefit for this purpose.

Dr. Cuffe should not find it difficult to accept that both experience and theoretical calculations indicate that induced surges are innocuous on systems of 33kV and above. This conclusion, of course, does not apply to lower-voltage systems, which are outside the scope of the paper. We should have to have detailed information of the lines involved in order to comment on the discrepancies between isoceraunic levels and lightning fault rates observed in Ireland.

The primary function of anti-fog shedding on insulators is not the prevention of contamination, as Mr. Bonar implies, but the improvement of performance, both by increasing the leakage path length and by providing conditions favourable for drying out a high proportion of the surface by leakage current. The prevailing view has been that leakage path length should increase roughly in proportion to voltage, but above 132kV it was thought that the increase might be rather more than linear; as a result 400in was specified for 380kV, but, so far, at this voltage, in experimental tests, performance has been rather better than feared.

The insulation value of wet wood for power-frequency voltages is probably insignificant.

Mr. Burke is unduly pessimistic. The lightning stroke is authoritatively regarded as a constant-current source, so that the potential at the point struck can be calculated with reasonable accuracy. Induced voltages caused by indirect strokes are voltages to earth; the phase conductors are effectively in parallel with respect to earth, so that the induced voltages on the phases do not add up to produce a higher resultant.

We agree with Mr. Edwards that experience alone will show whether the procedure for predicting line performance is valid. It will take many years before comprehensive fault statistics are available, and we claim that by putting forward an acceptable theory the collection and analysis of relevant data will be stimulated. The number of lightning incidents observed on the British 132kV Grid roughly agrees with prediction based on the formulae given.

As stated by Mr. Mather, the tower inductance determines the lower limit of earth resistance below which little benefit would accrue from further reduction. A lightning stroke discharging 20kA or more on the line would certainly cause flashover to one or more neighbouring conductors, and the current per phase would be reduced accordingly, so that a surge diverter would only be called upon to discharge the total current if the stroke terminated at the diverter itself or on the line close by. The latter contingency is avoided by lightning protection at and near the station.

As more data become available it will be possible and desirable to take waveform statistics into account in predicting performance.

On wood-pole lines without earth wires flashover, if it occurs, will usually be between two phases. It is, in any case, not to be expected that surge-diverter protection in stations can reduce or prevent trip-outs due to line flashover.

Mr. Thompson's contention, that the I.E.C. basic insulation levels should serve for line insulation in general, is fallacious. These standards are derived primarily from the characteristics of surge diverters. The choice of surge diverter for a given installation depends mainly on the highest system voltage, the residual discharge voltage and whether the system neutral is non-effectively or effectively earthed. These factors are not, in general, of first importance for line insulation where the main consideration is avoidance of outage by line flashover. The available statistics of lightning currents discharged by diverters are insufficient to justify inclusion in the paper. On the effect of rain we refer to our reply to Dr. Clark in London. In reply to his last question, lightning flashovers on the British 275kV system were 4 per 100 route-miles per annum, on the basis of one year's observation on a mean route-mileage of approximately 650. It should be added that the 132kV rate for the period was approximately twice the average.

As stated by Mr. Painter, evidence is accumulating, particularly in the United States, that systems operating at voltages above 132kV have a lightning fault rate which is higher than was expected. This is largely because, in estimating the expectation by, for example, the method of the American I.E.E., no account is taken of the effect of the height of the towers and conductors. If the E.R.A. method for estimating performance, as described in the paper, is used for a line with one earth wire, it is found that the 'area covered' when calculated by the formulae given in Section 2.4 is about twice as large for the 275kV as for 132kV system; in addition, the coupling factor is somewhat smaller owing to the larger spacings. The effect of these two factors is that the predicted fault rate for the 275kV system is about twice that for the 132kV system, a result which is roughly in agreement, so far, with experience in Great Britain. The choice of one mile as the length of line requiring protection in the neighbourhood of a station is a compromise which experience shows to be more or less well-founded; it has no theoretical basis.

The use of lightly insulated earth wire on a few towers would have no effect on the surge protection, but if a lightning stroke occurred at the position in question the insulation would probably be destroyed and the earth wire severely damaged.

The relationship of line insulation to station apparatus, mentioned by Prof. Carter, is dealt with in our reply to previous discussions. The standard 1/50 microsec waveform used for impulse tests has been adopted as the nearest equivalent to a lightning over-voltage which is technically feasible at present. Our comparison of the current-carrying capacity of an earth plate with that of a rod was intended to apply to equal weights of metal.

The uncertainty which attaches to the estimate of frequency of strokes to line, which is pointed out by Major Cunliffe, is

admitted. The figure given in the paper is believed to be the best which can at present be made. Great efforts are being made to improve on the figure by the collation and analysis of operating records. There is no firm evidence yet available with regard to the importance of induced surges for 33 kV systems.

We are in agreement with all of Mr. Szwander's remarks and in general with Mr. Bromley's, except that the economy obtainable from using an increased length of insulator string, which he proposes, is doubtful, since this calls for increased clearances and so increases the cost of the towers.

We apologize for giving a wrong impression of Mr. Dixon's opening remarks by associating him with Mr. Davis in our reply to the point in question. We were perhaps rather too categorical in this matter since, if it is already decided that substation protection is to be provided by rod gaps alone, this may well be a major consideration if a choice is required between unshielded wood-pole lines or metal poles with earth wires. As Mr. Dixon states, and as is mentioned in the paper, with unshielded, highly insulated lines it will generally be essential to use surge diverters for substation protection, since rod gaps may well be inadequate in this case.

A reduction of the rod-gap spacing to improve the surge protection obtainable may be possible in time, if high-speed reclosers can be used to counteract the increased outage frequency which would otherwise result, without detriment to performance in other respects. This, however, cannot be decided with certainty until more operating experience with these devices is available.

We are indebted to Mr. Gilmour for pointing out the error in the formula for the resistance of a rod in soil. [The constant should be 0·37 instead of 3·7.] Regarding the use of constants adjusted for feet and inches, we feel that, on balance, less chance of confusion is likely to arise by adhering to the more basic formula and converting dimensions to centimetres. The slightly more elaborate formula given by Professor H. B. Dwight, namely

$$R = \frac{\rho}{2\pi L}\left(\log_\varepsilon \frac{4L}{a} - 1\right) \text{ ohms}$$

can easily be shown to be equivalent to

$$R = \frac{\rho}{2\pi L}\left(\log_\varepsilon \frac{4L}{d} - 0\cdot 3\right) \text{ ohms}$$

where a is the radius and d is the diameter of the rod, and is slightly more accurate than the formula used in the paper—which is based on the assumption that the rod is an ellipsoid of revolution. However, the discrepancy between the two formulae in typical cases is less than 5%, and having regard to the general accuracy of these measurements this was considered to justify using the simpler formula, which incidentally has been well verified in practice.*

We agree that local breakdown of the soil caused by high voltage-gradients probably accounts for the lowered effective earth resistance for lightning discharge currents.

In the discussion at Newcastle upon Tyne, Dr. B. C. Robinson referred to the advantages obtainable from the use of an underrunning earth wire. We feel that our reply† is in need of amendment. Underrunning earth wires are, in fact, used almost exclusively on wood-pole lines, on which back-flashovers do not normally occur. Besides increasing the coupling factor and so reducing the amplitude of surges due to direct and indirect lightning strokes, they render an important service in reducing interference with telephone lines which may be strung underneath the earth wire.

In stating that the resistance of an earth electrode is increased by the passage of a heavy current, we were referring to a sustained current, as Mr. Hagenguth correctly assumes; the effect would not develop sufficiently rapidly to be a factor in multiple-stroke flashover.‡ On the other hand, the effective resistance to a high surge current may be less than normal, owing to local electric breakdown caused by the initial momentary over-voltage. Mr. Hagenguth's comment on surface temperatures may be correct, but it does not invalidate the accepted altitude correction factor given in the paper. Overhead line insulation which satisfies British and other specifications is suitable for use at altitudes up to 3 300 ft and at ambient temperatures up to 40° C, such temperatures being measured by a properly screened thermometer. If, owing to abnormal meteorological conditions, the ambient temperature at a high altitude is higher than is equivalent to 40° C at 3 300 ft, the condition of use is outside the specified limit, and for the purpose of calculating the test voltage applicable at sea level, say, a correction factor for temperature, in addition to the appropriate altitude correction factor, would be in order.

The problem described by Mr. Rao can hardly be regarded as falling within the scope of the paper. It appears that discharges across insulators of relatively low flashover voltage are caused by r.f. voltages superimposed on unidirectional voltages induced by indirect lightning or by thunderclouds. The use of insulators of higher flashover voltage and of surge diverters of appropriate rating should be remedial. On general grounds, it seems unlikely that the use of a common earth instead of a separate earth for the spark-gaps will make an appreciable difference to the effects observed.

* E.R.A. Report Ref. F/T50, 1932.
† *Proceedings I.E.E.*, 1957, 104 A, p. 247.
‡ TAYLOR, H. G.: 'The Current Loading Capacity of Earth Electrodes', *Journal I.E.E.* 1935, 77, p. 543.

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INDUCTION-MOTOR SPEED-CHANGING BY POLE-AMPLITUDE MODULATION

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SUMMARY

An original method of changing the speed of squirrel-cage induction motors has recently been developed which enables efficient and economic operation to be obtained at either of two chosen speeds, using a single winding of standard type.

The method has considerable generality, but is particularly applicable to speed ratios not very different from unity. The initial tests have therefore been performed on a motor for two speeds in the ratio 5 : 4, but a variety of ratios can be obtained by using the same principle. The control gear needed is extremely simple.

(1) INTRODUCTION

Methods of changing the speed of squirrel-cage induction motors, by rearranging the winding so as to change the number of poles, have long been known and used. In particular, motors for a 2 : 1 speed ratio have been specially favoured because of the simple control gear required, and one of the present authors has recently perfected two windings giving a 3 : 1 speed ratio.[1,2] In German literature[3] especially, windings for a number of speed ratios lying between unity and 1·5 have been described, but few are satisfactory as industrial propositions, if only because the control gear required is so complicated. These earlier methods of pole changing have, in general, been individual and special to each pole ratio, and little continuity of theme or logic can be discerned between the ideas which they embody.

The new method of speed changing described,[4] which has been given the name 'pole-amplitude modulation', ultimately results in a change in the number of poles produced by the winding; but it has considerable generality and flexibility, and it has thus been thought desirable to give it a new and distinguishing name. The name chosen describes the scientific basis of the method, rather than the effect produced. Pole ratios lying between unity and 1·5 are the best field for the application of this method, and a variety of pole ratios lying in this range can readily be obtained.

A great advantage of this method is that, in all, only six motor terminals, and a standard type of controller, are required to give the change of speed. As compared with a machine having two independent windings, the control arrangements are almost equally simple, the rating of a given frame is considerably higher, and the cost of manufacture is much less. For one of the two speeds (at choice) the rating of the frame is equal, or almost equal, to its full rating at that speed. For the other speed the rating is about 75% of the full rating for that speed. There is no difficulty in the manufacture of a machine working on this principle. The novelty lies in the design, and the construction and winding arrangements are of perfectly standard type.

It is not normally within the province of an academic engineer to prescribe applications for his research; but a few months ago, just after making tests on a small machine, it happened that one of the present authors actually saw several double-wound machines being manufactured, for fan drives in power stations, which could have been made more economically by this new method. These particular machines were designed for 8/10 poles, 833/455 h.p., 738/588 r.p.m., the power being nearly proportional to the cube of the speed. In such a case, the machine would be wound normally to give the required horse-power at the higher speed; and the same winding, when amplitude-modulated in one of the ways described below, would be capable of giving more than the reduced horse-power required at the lower speed. The small reduction in performance for the modulated connection, as compared with a normal winding, will not be of any consequence in applications such as this. The authors normally have no opportunities of making tests on large machines, but two manufacturers are now building large machines of this type. Nothing but full-scale tests can put any method beyond criticism, but there is little reason to suppose that the new method, which has been successfully used at 3/2 h.p., cannot be extrapolated to much larger sizes.

A number of further developments of the principle of pole-amplitude modulation are already under theoretical and experimental study, but it may be a year or two before a definitive account is ready. This paper is intended as a short enunciation of a new method of obtaining close-ratio speed-changing induction motors, which it is hoped will encourage interest in the principles of the method.

(2) THEORY OF POLE-AMPLITUDE MODULATION

The theory of pole-amplitude modulation can be generalized simply, as follows.

Suppose that one phase alone of a 3-phase winding gives a distribution of m.m.f. (and resultant flux) as shown diagrammatically in Fig. 1(*a*). (For simplicity the ideal sine half-waves are shown as rectangles.)

This can be expressed as a field strength $B_\theta = A \sin (p/2)\theta$, where A is the amplitude, and p is the number of poles. In the Figure, p is equal to 8. Suppose the pole-amplitude A were modulated in space by making A take the form $C \sin k\theta$, where k may be any integer, but, in the Figure, it is made equal to unity.

We should then have a field given by

$$\begin{aligned} B_\theta &= A \sin (p/2)\theta \\ &= C \sin k\theta \sin (p/2)\theta \\ &= \frac{C}{2}\left[\cos\left(\frac{p}{2} - k\right)\theta - \cos\left(\frac{p}{2} + k\right)\theta\right] \end{aligned}$$

This indicates, in general, a double field of two pole numbers, $(p - 2k)$ and $(p + 2k)$, respectively, superimposed, one pole number being greater than the original and one less. In the particular case considered, the resultant field B_θ will be a mixture of a 6-pole and a 10-pole field, as can in some degree be seen from Figs. 1(*d*) and 1(*e*), although the 6-pole component is the more apparent to the eye.

There are two methods of connection by which the desired modulation can be obtained simply in practice. In the first method, the second half of each phase winding is reversed as a

Written contributions on papers published without being read at meetings are invited for consideration with a view to publication.

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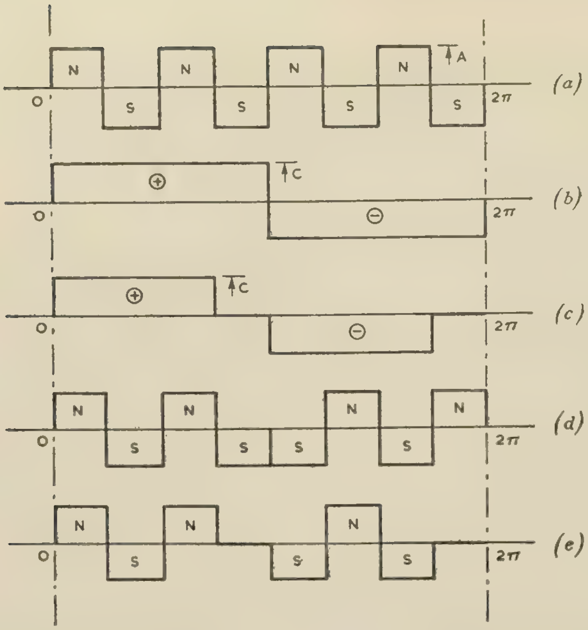

Fig. 1.—Principle of pole-amplitude modulation.
Eight poles modulated to 6/10 poles.

whole with respect to the first half; in the second method, a section is first omitted from each half, and half the remaining portion is then reversed with respect to the other half. (In the particular case considered, the fourth and eighth groups of coils are omitted, and the fifth, sixth and seventh are then reversed with respect to the first three groups.) These two changes of connection are, respectively, equivalent to multiplication of the original wave by a rectangular wave, of one or other of the forms shown in Figs. 1(*b*) and 1(*c*). These rectangles can each be represented by a Fourier series of odd terms, of which the first is, of course, much the largest. The method of modulation actually employed therefore modulates the original wave not only by the ideal factor $C \sin \theta$, but also by a series of factors $C_\lambda \sin \lambda\theta$, where $\lambda = 3, 5, 7$, etc. Subject to certain reservations, however, it has been found possible to ignore all but the fundamental modulation and the poles which it produces.

A space-modulated field, containing two pole numbers mixed, would, in itself, be of little use; and it is clearly necessary to eliminate one of the two pole numbers whilst retaining the other at full effectiveness. It is shown in Section 10 (Appendix) that when the single-phase field of two pole numbers mixed, arising from the modulated phase winding, is combined with the mixed field of two other phase windings, the resultant can be made to be a 3-phase field of one pole number only. In order to achieve this, the original windings, before pole-amplitude modulation, must be spaced electrically by $r(2\pi/3)$, where r is given by one of the following equations. In a standard induction motor r will normally be made equal to unity, though it can have any integral value other than 3 or a multiple of 3.

The pole numbers in the single-phase modulated field are $(p + 2k)$ and $(p - 2k)$, and the condition that the former or the latter, respectively, can be made to vanish in the 3-phase resultant field is that, identically and exactly,

$$\frac{m}{r} = \frac{1}{3}\left(1 + \frac{2k}{p}\right); \quad \text{or} \quad \frac{m}{r} = \frac{1}{3}\left(1 - \frac{2k}{p}\right)$$

where p is the number of poles in the original unmodulated winding, $2k$ is the number of poles to be added or subtracted, and m is any integer. It is also shown in Section 10 (Appendix) that the condition for the one pole number to vanish is the same as the condition for the other pole number to appear at its full value in the 3-phase resultant field. It thus becomes possible, when these conditions can be satisfied, to change one pole number to another by the process of space modulation of pole amplitude, which can be carried out by changing the connections in one of many ways foreshadowed above.

Pole combinations which meet the conditions given above include

8/10 : 14/16 : 20/22 : 26/28 : $(6n + 2)/(6n + 4)$: etc., for $k = 1$

and 10/14 : [16/20] : 22/26 : [28/32] : $(6n + 4)/(6n + 8)$: etc., for $k = 2$

and [24/30] : [42/48] : [60/66] : [$(18n + 6)/(18n + 12)$] : etc., for $k = 3$

and 14/22 : [20/28] : 26/34 : [32/40] : 38/46 : $(6n + 8)/(6n + 16)$: etc., for $k = 4$

It will be observed that some of these pole ratios (those enclosed in square brackets) for $k > 1$ are simply multiple repetitions of ratios previously obtained. All these combinations may be expressed generally either as $(6n + 2k)/(6n + 4k)$, where k is not a multiple of 3, or as $k(6n + 2)/k(6n + 4)$, where k is a multiple of 3. The initial term for each of the four series of possible pole ratios given above is obtained by writing $n = 0$ in the corresponding general expressions. The first term in each series is thus seen to be, respectively, 2/4 : 4/8 : 6/12 : 8/16; which are all in the ratio 1 : 2. The familiar 2 : 1 pole-changing winding can thus be considered as a particular case of the general principle of pole-amplitude modulation; and when the method used for such pole changing is considered, it becomes clear that it is, in effect, a simple example of pole-amplitude modulation. The first term in any series of possible pole ratios is equal to $2k/4k$.

Each series of possible pole ratios has a fixed difference between the two pole numbers, the difference being, respectively, 2, 4, 6, 8, etc. For those series of pole ratios where the difference is not a multiple of 3, it will be observed that the possible pole ratios are those in which neither pole number is itself a multiple of 3. Where the difference is a multiple of 3, only a few pole ratios can be obtained, the possibilities being limited to those which are integral multiples of the ratios which can be obtained when the difference in pole numbers is 2.

The authors' initial experiments have been performed on a small motor for the 8/10 pole combination, and it has proved entirely satisfactory. For this machine $p = 8$, $k = 1$; and $m = 1$, $r = 4$ therefore satisfies the equation for the lower pole number, 6, to vanish. The result of modulating a 3-phase 8-pole field can therefore be a 3-phase 10-pole field.

The principle which enables one pole number to be eliminated may be further explained physically, as follows. If the electrical spacing between three similar sinusoidally distributed windings is $2m\pi$, where m is any integer, and the windings are fed with 3-phase current, the net m.m.f. exerted is always zero at all points. In any pole-changing winding the same physical displacement between three phase-windings corresponds to different electrical spacings, according to the pole number of each winding; and by changing the pole number the effective spacing is altered, without altering the mechanical displacement. In the same way, a winding which produces two pole numbers simultaneously has different effective spacings for these two numbers. Therefore, if the three phase-windings of a symmetrical complex 3-phase winding, of which each phase-winding sets up an m.m.f. of two pole numbers mixed, are spaced elec-

trically by $2m\pi$ with respect to one of these pole numbers, there will be no net m.m.f. for that number of poles, and the complete winding will produce a resultant field of the other pole number alone.

It is believed that the principle of mixed pole numbers has previously been applied only in relation to the d.c. pole system of a multipole alternator.[5] In this case, the excitation coils of certain of the poles were magnetically neutralized, and half of the remaining coils were reversed with respect to the other half. In effect, the d.c. field was modulated to give a field of two pole numbers mixed. The separation of the two resultant e.m.f.'s in the armature was carried out by winding it as an elaborate pole-changing system, with 18 terminals and 18 connecting leads, so that it responded to the field of one pole number and not to that of the other. It is probable that the methods of pole-amplitude modulation discussed in this paper could also be applied to the armature of an alternator, where the pole combination required is one of those to which the methods of this paper are applicable.

(3) HARMONICS IN MODULATED WAVE

Each phase winding, when modulated, only repeats itself once per modulation cycle. For machines whose pole numbers differ by 2, this means that there will be only one complete cycle of m.m.f. in travelling round the whole machine. The basic pole number of the modulated winding is therefore 2; and it is possible to consider a machine with such a winding to be a 2-pole machine with very weak fundamental poles, in which an extremely heavy harmonic is produced by pole-amplitude modulation, the machine being intended to operate on this harmonic. The fundamental (2-pole) field rotates in the same direction as its seventh, thirteenth, etc., harmonics: but the fifth harmonic rotates in the same direction as the eleventh, seventeenth, etc., harmonics. Therefore, if a machine were intended to operate on what is, in essence, an exaggerated fifth harmonic, it might possibly crawl on the eleventh or seventeenth harmonic, if these were sufficient in magnitude. As usual, the lowest order of harmonic is the most significant, and theory thus led to the expectation that, if the test machine for 8/10 poles crawled at all, it would do so at 5/11 of its 10-pole synchronous speed.

This, in fact, proved to be the case with the first machine, which was wound with 8-pole full-pitch coils. If it was started in the normal 8-pole connection and then modulated to 10 poles, crawling was, of course, avoided, and the machine ran normally in the modulated connection. Normal winding theory shows that, if this 8-pole winding is chorded to two-thirds full pitch, the eleventh harmonic (relative to 2 poles) will be reduced to a small fraction of what it was; and a second machine wound with 8-pole coils, of two-thirds full pitch, but otherwise similar to the first, had no crawling tendencies. The relative sizes of the harmonics are given in Fig. 9.

This consideration of the machine as operating on an exaggerated harmonic leads to another possible way of explaining the general principle of pole-amplitude modulation. It is well known, by standard polyphase winding theory, that triplen harmonics in the m.m.f. due to one phase-winding do not appear in the resultant m.m.f. of the three phase-windings; but that all other harmonics are reproduced in the resultant, in their original proportion to one another. Therefore, if a single phase-winding produces by modulation a mixture of two pole numbers, one of which is a triplen harmonic of the basic modulation cycle, the latter pole number will be eliminated when the three phase-windings are combined, and the machine will operate at a speed corresponding to the other pole number. It has already been observed that the resultant pole number in single modulation can never be a multiple of 3, which accords exactly with the concepts which have just been discussed.

It ought perhaps to be emphasized in conclusion that, when operating on the fifth harmonic, the fundamental (2-pole) field and the seventh, thirteenth, etc., harmonic fields must be regarded as of negative rotation whereas conversely the fifth, eleventh, etc., harmonic fields are of positive rotation.

(4) A RECIPROCAL PRINCIPLE IN POLE-AMPLITUDE MODULATION

An examination of the algebraic results given in Section 10 (Appendix) will show that a normal unmodulated winding of $(6n + 2)$ poles can be singly modulated $(k = 1)$ to add two more poles and thus to give a resultant field of $(6n + 4)$ poles. The other pole number $6n$, produced by modulating each phase individually, vanishes when the three phases are combined. However, if the original unmodulated winding is constructed normally for $(6n + 4)$ poles, the product of modulating each phase will be a field of $(6n + 6)$ poles and $(6n + 2)$ poles mixed; but in this case, as shown in Section 10 (Appendix), it is the higher pole number $(6n + 6)$ which must vanish, leaving a modulated resultant field of $(6n + 2)$ poles.

It is therefore clear that, for single modulation $(k = 1)$, it is possible either to start with a normal winding of $(6n + 2)$ poles and to increase this to $(6n + 4)$ poles by modulation, or to start with a winding of $(6n + 4)$ poles and to reduce this to $(6n + 2)$ by modulation. This is a reciprocal principle of general application to single, double or multiple modulation. It will usually be best to start with the smaller number of poles and to increase it by modulation, because the power rating when the winding is modulated is a little less than for a normal winding of comparable size. Commonly, the load demanded falls off rapidly as the speed falls, and it is likely that modulation to increase the pole number will find more applications than modulation to decrease it. However, the reciprocal relationship ought to be recorded.

(5) METHODS OF MODULATION, PHASE INTERCONNECTION AND SWITCHING

It was explained in Section 2 that pole-amplitude modulation could be effected for each phase either by reversing one half of the phase with respect to the other half, which corresponds to Figs. 1(*b*) and 1(*d*); or by cutting out a section from each half and reversing the remainder of one half with respect to the remainder of the other half, which corresponds to Figs. 1(*c*) and 1(*e*). The second method, which involves cutting out part of each phase winding, can be put into practice either by directly removing the portions concerned from the circuit, or by neutralizing them magnetically but leaving them still in circuit. Removal is better where it can readily be arranged, because neutralization involves waste I^2R loss in the coils concerned.

When modulation is obtained by simple reversals, according to the principle of Figs. 1(*b*) and 1(*d*), it is possible to use series circuits in the modulated connection and parallel circuits for the normal connection, or vice versa, as shown in Fig. 2. Only three leads out per phase are needed, and it is possible to use parallel-star/series-delta switching or parallel-star/series-star switching for the complete 3-phase winding, just as is done for 2 : 1 pole-changing arrangements; and, as shown in Fig. 3, only six terminals and six external leads are finally required.

Two of the present authors[6] have shown that it is possible, without requiring extra terminals, to add additional coils to each phase of a normal 2 : 1 pole-changing winding, when connected for one of its pole numbers, using series-parallel switching of the windings. The coils to be added are connected to the centre of

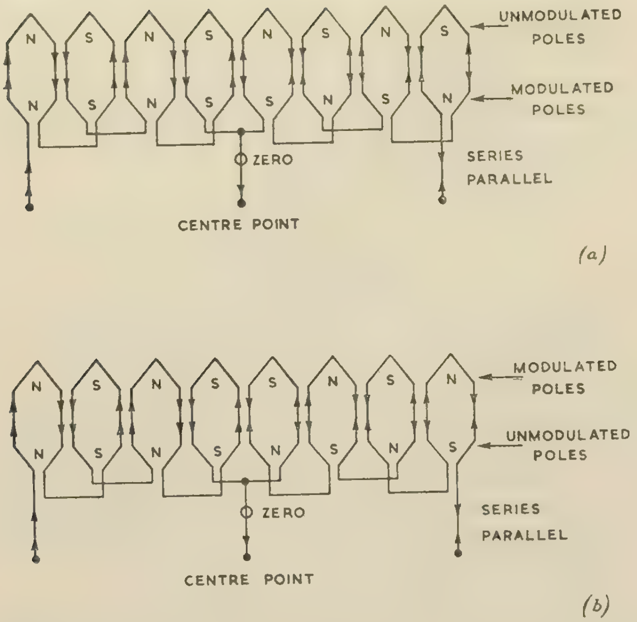

Fig. 2.—Pole-amplitude modulation by series-parallel switching.

Layout and connections of coil groups of one phase:
(*a*) Series unmodulated: parallel modulated.
Three leads out per phase.
(*b*) Series modulated: parallel unmodulated.
Three leads out per phase.

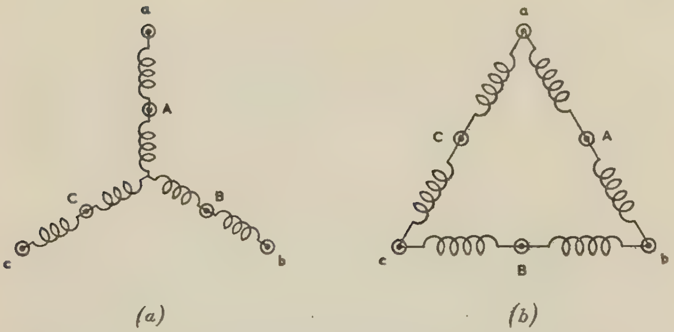

Fig. 3.—Pole-amplitude modulation by reversal of complete half-windings.

(*a*) Parallel-star/series-star either modulated/unmodulated or unmodulated/modulated.
(*b*) Parallel-star/series-delta either modulated/unmodulated or unmodulated/modulated.

First connection	Supply	A	B	C	⊙ Six control leads
	Join	*a*	*b*	*c*	
Second connection	Supply	*a*	*b*	*c*	
	Isolate	A	B	C	

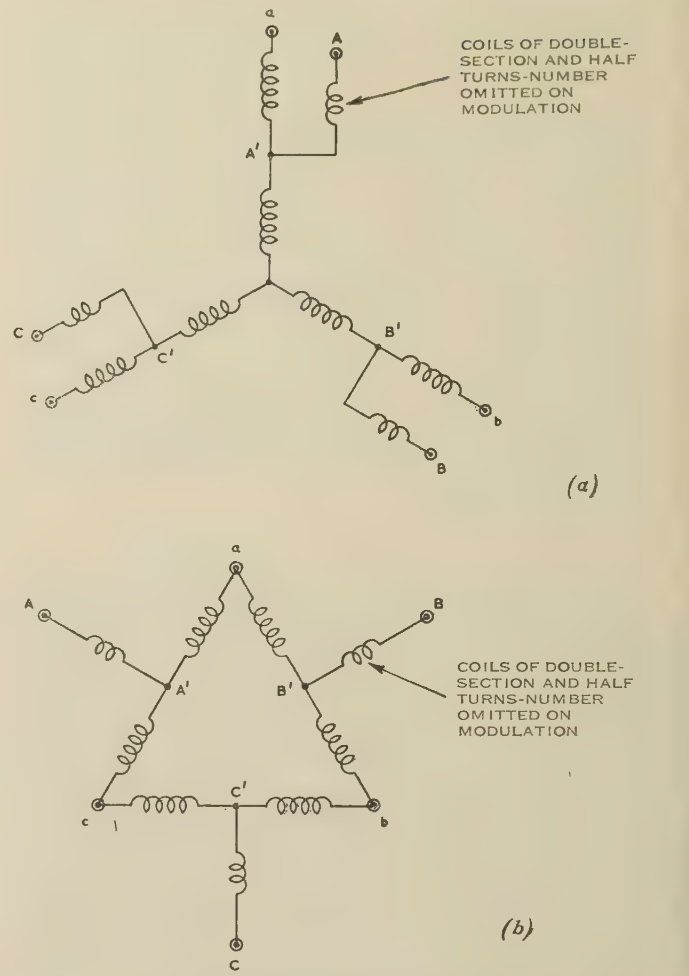

Fig. 4.—Pole-amplitude modulation with partial winding omission.

Parallel circuits when unmodulated.

(*a*) *Unmodulated*					
Parallel-star with extra coils.	Supply	A	B	C	⊙ Six control leads
	Join	*a*	*b*	*c*	
Modulated					
Series star.	Supply	*a*	*b*	*c*	
	Isolate	A	B	C	
(*b*) *Unmodulated*					
Parallel-star with extra coils.	Supply	A	B	C	⊙ Six control leads
	Join	*a*	*b*	*c*	
Modulated					
Series-delta.	Supply	*a*	*b*	*c*	
	Isolate	A	B	C	

the rest of the coils and are thus included when the rest of the winding is parallel connected, and are cut out when the rest is series connected. The parallel connection must therefore always be a star connection also. The series connection may be in star or in delta, as shown in Figs. 4(*a*) and 4(*b*).

This method of omitting coils in one connection and including them in the other may also be used for obtaining pole-amplitude modulation in a way which corresponds to the principle of Figs. 1(*c*) and 1(*e*), the coil connections for one phase being shown in Fig. 5(*a*). Again, only three leads per phase are needed, and the two possible interconnections of the three phases are shown in Figs. 4(*a*) and 4(*b*). Since coils are to be omitted on modulation, it follows that parallel-star circuits must be used for the normal connection and series circuits for the modulated connection, either in star or in delta. Again only six terminals and six external leads are required.

If modulation is to be obtained by magnetic neutralization of some coils, which also corresponds in principle to Figs. 1(*c*) and 1(*e*), it is necessary for these particular coils to be wound in two halves. Each phase is connected as shown in Fig. 6, and once more only three leads out per phase are needed; and the same kinds of switching (shown in Fig. 3) can be used as when modulation according to the principle in Figs. 1(*b*) and 1(*d*) is being carried out. Again only six terminals and six external leads are required. This was the circuit used by Tittel[5] for obtaining modulation of a d.c. field pole system; though it is inherently a thermally wasteful circuit.

A special case arises when modulation with coil elimination, corresponding to Figs. 1(*c*) and 1(*e*), is being carried out, but it is desired that parallel circuits shall be used in the modulated connection and series circuits in the normal connection. Each phase is then connected as shown in Fig. 5(*b*), with four leads

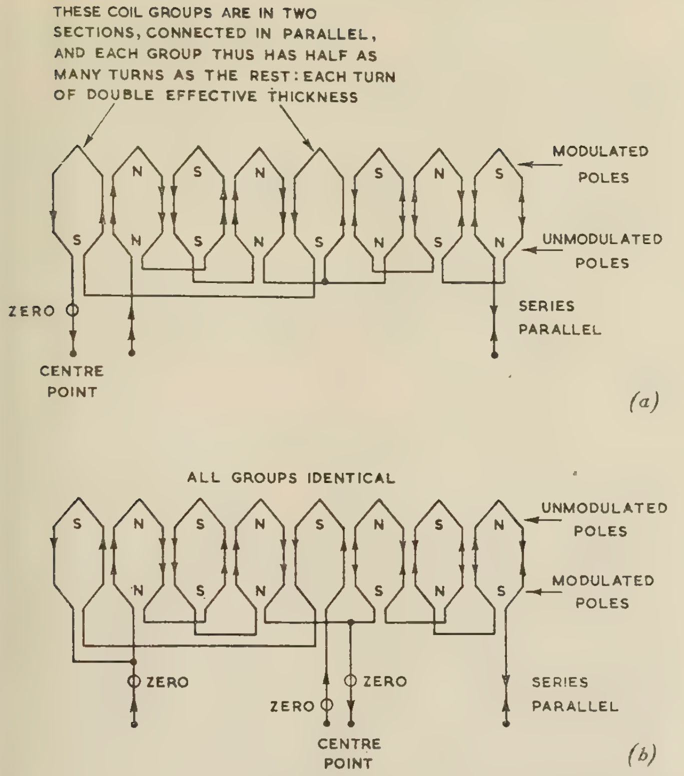

Fig. 5.—Pole-amplitude modulation by series-parallel switching, with partial winding omission.

Layout and connections of coil groups of one phase:
(*a*) Series modulated : parallel unmodulated. Three leads out per phase.
(*b*) Series unmodulated : parallel modulated. Four leads out per phase.

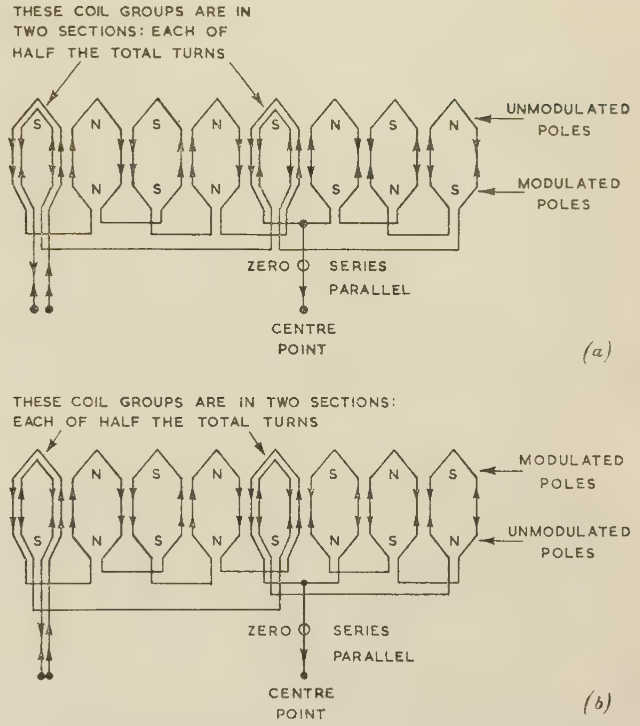

Fig. 6.—Pole-amplitude modulation by series-parallel switching, with partial winding neutralization.

Layout and connections of coil groups of one phase:
(*a*) Series unmodulated : parallel modulated. Three leads out per phase.
(*b*) Series modulated : parallel unmodulated. Three leads out per phase.

Fig. 7.—Pole-amplitude modulation with partial winding omission. Parallel circuits when modulated.

Unmodulated.	Series star	Supply	A	B	C				⊙ Nine control leads
		Isolate	A′	B′	C′	*a*	*b*	*c*	
Modulated.	Parallel-star with coils omitted	Supply	*a*	*b*	*c*				
		Join	A′	B′	C′				
		Isolate	A	B	C				

out per phase, and it is necessary to retain nine terminals and nine external leads for the complete winding. This method of modulation, the circuit for which is shown in Fig. 7, would be used, however, only in those rare cases when all the others were inapplicable. Further, it is desirable in this case to use parallel-star/series-star connection, because parallel-star/series-delta connection is impossible without increasing the number of terminals and external leads to twelve.

These different methods of pole-amplitude modulation and phase interconnection enable different relative flux densities at the two speeds to be obtained. It is intended in due course to ascertain by theory and experiment the performances given by these various options in a number of representative machines, in order to find which is best for each particular type of application. However, there is a considerable degree of freedom in design when the various options for pole-amplitude modulation, phase interconnection and switching are all considered; and this freedom gives every chance of optimizing the performance at both speeds. The connections used, and the resultant relative flux densities, for the test machines are discussed in Section 6.1.

For the particular case of single modulation only, there is one respect in which the connections of these windings differ from what has sometimes been regarded as good standard practice. For either the modulated or unmodulated connection, most of the winding, at least, must be connected to have two parallel paths; and, because of the method of modulation used, these two paths must each be composed of an equal number of consecutive adjacent coil groups on opposed sides of the machine frame, and it is not possible to connect successive coil groups alternately in the two paths. If the machine is, in fact, geometrically perfect, and there is thus no asymmetry in the air-gap, there will be no difference whatever between the results obtained from the two methods of connection. Whilst it may be best to adhere, where possible, to the practice of connecting successive groups in alternate paths, the present authors incline to the opinion that this practice, which equalizes the magnetizing currents in the two paths, was determined at a time when manufacturing tolerances were much wider than they now are, and

when the wear of sleeve bearings often led to considerable asymmetry in air-gap length. At any rate, the authors' test machines have necessarily been connected with parallel circuits each containing consecutive adjacent coil groups, but the unbalance between parallel paths has been trivial and no ill effects have resulted from this.

At the most, it seems possible that large machines which are to use this method of speed control ought to be built with just a little extra care in manufacture to ensure geometrical symmetry; in small machines any objection on this ground can almost certainly be discounted. Further, where double or multiple modulation is to be used it once more becomes possible to distribute the coils in each parallel path symmetrically around the machine perimeter, by appropriate connection of the second space-cycle of modulation with respect to the first.

(6) TEST MACHINES AND RESULTS OBTAINED

(6.1) Windings Used and Resultant Flux Densities

For winding the test machines, totally enclosed fan-cooled frames were used, each being rated by its makers as a 3 h.p. frame for a standard 8-pole winding, and as a 2 h.p. frame for a standard 10-pole winding. There were 48 stator slots (two slots per pole per phase for eight poles), and a squirrel-cage rotor with 54 slots, skewed one stator slot pitch. The first machine frame was wound with full-pitch coils (six slot pitches) and the second with two-thirds full-pitch coils (four slot pitches), both being double-layer diamond windings. The second machine frame with two-thirds full-pitch windings had 56 turns per coil of No. 20½ s.w.g. Lewmex Bakelized enamelled wire, and was suitable for operation on 440 volts (line) 3-phase 50 c/s. The permitted temperature rise (50° C) was measured by a thermocouple embedded in one of the centre coils, and the machines were therefore being judged under the most unfavourable condition of measurement. Other temperatures were measured by thermometers and found to be lower.

All the coil groups were identical, and each test machine had its 24 coil groups connected to 48 terminals on an external board, so that it could readily be reconnected in a variety of ways. The connection shown in Fig. 5(*a*) was obtained for eight poles, for test purposes, by joining two normal coil groups in parallel, instead of winding two special coil groups, each of half the number of turns of double gauge connected in series, as shown in the Figure. This was permissible since, in the test machines, the 8-pole coil groups which are omitted for 10-pole operation can then be left open-circuited. If these coil groups were left connected in parallel during 10-pole operation there would be a heavy circulating current, and the arrangement shown in Fig. 5(*a*) would be used in most cases in any permanently connected form of the machine. There is, however, usually no difficulty in doing so, and the performance will be identical with that of the test machines. In larger low-voltage machines, however, the number of conductors is small, and may be odd. It is then necessary to make all the coils of the same size; and to connect in parallel for 8-pole operation those coils which are omitted for 10-pole operation. In this case three extra control leads, making nine in all, will be required; and it could be a matter for discussion whether providing three extra leads is not cheaper than arranging for some coils to have different design details. On the other hand, reduction of the leads to six is a great simplification in operation.

Both machines were tested with both methods of modulation, corresponding to the principles of Figs. 1(*b*) and 1(*c*), the coil group connections being given in Figs. 2(*b*) and 5(*a*). The phase interconnections, for 8/10 poles, respectively, were parallel-star/series-star, corresponding to Fig. 3(*a*) or 4(*a*) for the two methods of modulation.

In calculating the winding factors for a machine which uses a modulated connection, there is an additional factor to be considered besides the usual spread and chord factors. In a normally connected machine, the spacing of successive coil groups is always made equal to a pole pitch, but after modulation this will no longer be the case. In the test machines, the electrical angle between successive coil groups after modulation is clearly $5\pi/4$, in relation to the 10-pole field; and, in general, the angle is $m\pi/4$, where m is the order of any harmonic in relation to the basic 2-pole field. As has been observed in Section 3, the main field of these machines is really a fifth harmonic of 2 poles, and m must be taken as equal to 5.

The e.m.f.'s in all the coil groups are therefore not arithmetically additive. When added vectorially, their sum is less than the arithmetic sum, and the ratio

$$\frac{\text{(Resultant e.m.f. of coil groups in series)}}{\text{(Number of groups)} \times \text{(E.M.F. per group)}}$$

is called the connection factor. This has been discussed, together with its application in computing winding factors and waveform analyses, in an earlier paper by Burbidge.[7]

This new factor may be compared in some respects with the spread factor. For a given angle between single coils, their spread factor steadily diminishes as the number in series becomes greater; and, in the same way, the connection factor of a set of coil groups diminishes as the number of coil groups in series is increased.

By the methods described by Burbidge,[7] the 10-pole connection factor for the test machines can be shown to be $\sin(m\pi/8)\cos(m\pi/4)\sin(m\pi/2)$, with all coils in circuit; and $\frac{1}{3}[2\cos(m\pi/4) - 1]\sin(m\pi/2)$ with one quarter of the coils omitted.

The spread factor can easily be seen to be $\cos(m\pi/48)$ for both machines, and the chord factors to be $\sin(m\pi/8)$ and $\sin(m\pi/12)$ respectively, where m is again the order of harmonic with respect to a 2-pole fundamental. The four winding factors for the two machines are therefore as follows:

Full pitch winding	(*a*) Modulation by complete reversal ..	$\cos\frac{m\pi}{48}\sin^2\frac{m\pi}{8}\cos\frac{m\pi}{4}\sin\frac{m\pi}{2}$
	(*b*) Modulation with coil omission ..	$\frac{1}{3}\cos\frac{m\pi}{48}\sin\frac{m\pi}{8}\left(2\cos\frac{m\pi}{4} - 1\right)\sin\frac{m\pi}{2}$
Chorded winding	(*c*) Modulation by complete reversal ..	$\cos\frac{m\pi}{48}\sin\frac{m\pi}{12}\sin\frac{m\pi}{8}\cos\frac{m\pi}{4}\sin\frac{m\pi}{2}$
	(*d*) Modulation with coil omission ..	$\frac{1}{3}\cos\frac{m\pi}{48}\sin\frac{m\pi}{12}\left(2\cos\frac{m\pi}{4} - 1\right)\sin\frac{m\pi}{2}$

The 10-pole winding factors are given by putting $m = 5$ in these expressions, with the results successively (*a*) 0·572, (*b*) 0·705, (*c*) 0·600 and (*d*) 0·736. It will be seen that the omission of coils improves the winding factor of each machine, because of the increase of connection factor, as mentioned previously.

The winding factors for 8-pole operation are readily calculated according to standard principles, and are found to be 0·966 for the first machine, and $(\sqrt{3}/2)(0\cdot966)$ for the second machine.

If B_8 and B_{10} are the air-gap flux densities for the two pole numbers, it follows that, in machines (*a*) and (*c*),

$$\frac{B_8}{B_{10}} = 0\cdot8 \times 2 \times \frac{\text{10-pole winding factor}}{\text{8-pole winding factor}}$$

and in machines (*b*) and (*d*), where only three-quarters of the winding is in circuit for 10-pole operation,

$$\frac{B_8}{B_{10}} = 0\cdot75 \times 0\cdot8 \times 2 \times \frac{\text{10-pole winding factor}}{\text{8-pole winding factor}}$$

Using these expressions and the winding factors already deduced, it is found that B_8/B_{10} in the four machines has the respective values (*a*) 0·945, (*b*) 0·875, (*c*) 1·14 and (*d*) 1·06. These ratios are all very satisfactorily near to unity.

The magnetizing curves for the preferred machine (*d*) are given in Fig. 8, from which it will be seen that the ratio of phase currents at the operating voltage, for 8/10 poles, respectively, is 2·45/2·05 = 1·20. Since the 8-pole connection uses parallel circuits, the ratio between the conductor currents I_8/I_{10}, for the two pole numbers, is 0·60. For the same flux density at both pole numbers, the ratio I_8/I_{10} should theoretically be the product of the ratio of the pole numbers multiplied by the inverse ratio of the effective numbers of conductors. This product here has the value

$$\frac{8}{10} \times \frac{0\cdot75n \times 0\cdot736}{n \times 0\cdot836} = 0\cdot528$$

Since the flux-density ratio B_8/B_{10} is 1·06, the theoretical value of I_8/I_{10} is $(1\cdot06 \times 0\cdot528) = 0\cdot56$. Fig. 8 therefore shows both

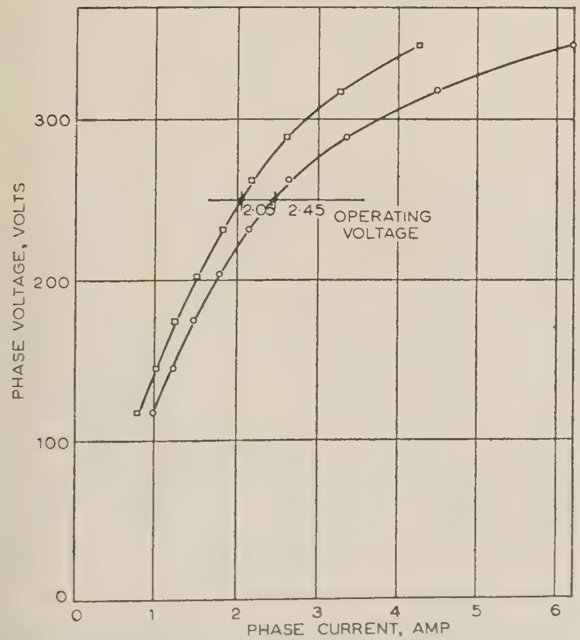

Fig. 8.—Magnetizing curves: Final 8/10 pole machine (*d*).
○ ○ ○ 8-pole parallel-star.
□ □ □ 10-pole series-star.

that the magnetizing-current ratio is fully acceptable in practice, and that it very nearly conforms with theory. Altogether, therefore, the magnetic loadings and magnetizing currents for the two pole-numbers in the final machine may be regarded as very well proportioned.

The maximum voltages which were found to be respectively permissible for the two windings, without causing saturation and excessive magnetizing currents, were, however, not in the theoretical ratio; i.e. in the ratio of the number of effective conductors. For the same number of effective conductors in both machines, the voltage on the chorded winding could be raised to 440 volts, as compared with 400 volts on the full-pitch winding. It seems clear that the permissible voltage for the full-pitch winding was lowered below the first-order theoretical value because of the presence of such large harmonic fluxes that simple methods of computation, which have regard to the fundamental flux only, became widely inaccurate. The test results for ratings given below were determined on this unequal voltage basis. It ought, however, to be emphasized in conclusion that machine (*d*) alone is put forward as a satisfactory industrial proposition. The earlier machines (*a*), (*b*) and (*c*) are discussed only in order to explain logically the steps by which a satisfactory machine was finally obtained.

(6.2) Full-Load Test Results

The test results obtained for continuous output ratings are given in Table 1, the machines being operated at 10-pole speed

Table 1

SUMMARY OF TEST RESULTS FOR CONTINUOUS RATING

Machines with full-pitch coils: Line voltage, 400 volts		Machines with two-thirds full-pitch coils: Line voltage, 440 volts	
Machine (*a*)	Machine (*b*)	Machine (*c*)	Machine (*d*)
All coils in circuit	One coil in four omitted	All coils in circuit	One coil in four omitted
1·00 h.p.	1·25 h.p.	1·40 h.p.	1·60 h.p.

(*a*) → (*b*)	(*b*) → (*c*)	(*c*) → (*d*)
Rating improvement due to reduction of 2-pole and 14-pole fields. Higher harmonics are almost unaltered.	Rating improvement due to reduction of higher harmonics, and to the permissible increase in voltage. Two-pole and 14-pole fields become larger and tend to worsen the rating	Rating improvement due to reduction of 2-pole and 14-pole fields. Higher harmonics are almost unaltered

in each of the two modulated connections in turn. The Fourier analyses of the m.m.f. waveforms arising from the four types of winding were carried out by the simple method recently described by Burbidge,[7] and the amplitudes of the various components are shown in Fig. 9. As explained in Section 3, the possible Fourier components in the modulated connection are the fundamental (2-pole), and the fifth, seventh, eleventh and thirteenth, etc., harmonics, the machine being intended normally to operate

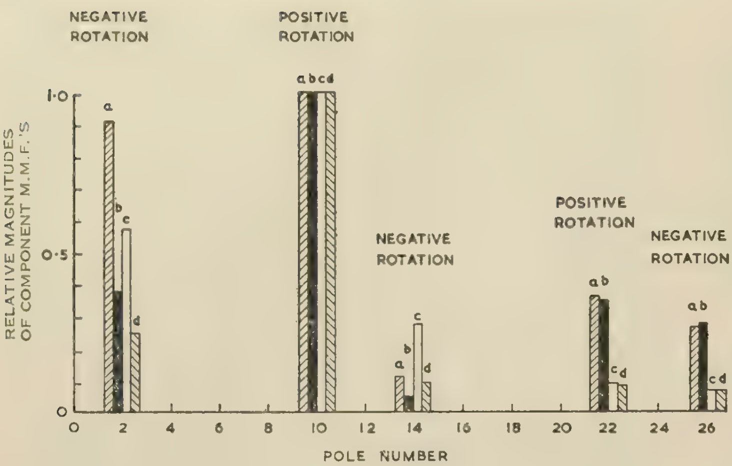

Fig. 9.—Relative magnitudes of the component m.m.f.'s in modulated (10-pole) machines.

(*a*) Full-pitch windings. All coils in circuit.
(*b*) Full-pitch windings. One quarter of coils omitted.
(*c*) Two-thirds full-pitch windings. All coils in circuit.
(*d*) Two-thirds full-pitch windings. One quarter of coils omitted.

on its large fifth harmonic. It will be seen that, in general, the harmonic content of the m.m.f. becomes steadily less when passing, in comparison, from machine (*a*) to machine (*d*) (see Table 1), except that there is an increase in the 2-pole and 14-pole fields when passing from machine (*b*) to machine (*c*). The improvements in continuous rating which take place can thus be clearly related to the harmonic contents of the m.m.f. waveforms, and notes are given below Table 1 which relate the trend in rating to these various harmonic contents. It is clear enough that large negative-rotation 2-pole fields and large subsynchronous fields both tend to increase the losses, and thus to reduce the rating. The output power obtained in the four tests provides strong evidence of the relative magnitudes of parasitic torques and stray losses in the four machines. It is broadly clear that their effect was serious in machine (*a*), but relatively very small in machine (*d*), which was found to give a continuous rating for 10-pole operation equal to more than 75% of the makers' rating for the same frame with a normal 10-pole winding. The 25% reduction in rating is sufficiently explained by the reduced winding factor alone.

The authors would have liked to take dynamic speed/torque curves, but they do not yet possess the elaborate and expensive apparatus needed for this purpose. They have therefore had to content themselves in this preliminary study with theoretical investigations of harmonics, together with complete tests under no-load and full-load conditions.

The most unusual feature of the m.m.f. waveform in an induction motor with pole-amplitude modulation is the presence of a negative-rotation sub-harmonic field, having two poles in the case of single modulation. This field may, in some degree, reduce the starting torque of such a machine, but it will not cause dips in the speed/torque curve, and the rotor will normally be running at about 120% slip in the 2-pole field. If this field is too strong it may cause excessive losses and noise; and it is to the reduction of the 2-pole negative-rotation field that particular attention has to be directed in machines of this type.

As mentioned in Section 3, the first test machine, with full-pitch coils, was found to crawl at 5/11 of its desired speed, when started in the 10-pole connection. It was therefore decided to use a chorded winding in order to reduce the eleventh harmonic and thus avoid crawling; but chording had also the most desirable effect of greatly reducing the 2-pole field, as shown in Fig. 9. The Fourier analyses also showed that omission of one-quarter of the coils from either winding further reduced the 2-pole field; and machine (*d*) which has chorded windings, and one-quarter of the coils omitted in the 10-pole connection, is much the best of the four alternatives, as shown theoretically by Fig. 9 and confirmed by the test results in Table 1.

It is a surprising and fortunate fact that, when only three-quarters of the total number of conductors are in circuit, their winding factor is higher than the winding factor of all the conductors taken together. The reduction in the effective number of conductors is therefore less than one-quarter, whereas the heating is reduced by a quarter. One of the present authors has elsewhere advocated[2] that the possibility that advantages may arise by omitting conductors should not be ignored, and this machine is a clear example of such a possibility.

Short-circuit tests were also performed on the machine in the 10-pole connection; and the pull-out horse-power, for both methods of modulation, was found to be approximately 2·3 h.p. If an overload-torque capacity equal to twice the full-load torque were really essential, this, rather than the continuous rating, would therefore be the limiting factor in the output obtainable from the frame. In many applications, however, this point would be of no substance.

It remains to add that the rating of the machine when normally connected for 8-pole operation was found experimentally to be almost exactly 3·0 h.p., as claimed by the makers of the frame. The currents in the two parallel paths were separately measured, and were found to be equal within a tolerance of about ±6%; and there were no observable ill effects, such as noise or vibration, arising from the use of parallel paths not symmetrically disposed around the machine perimeter. At least in small machines, the possible objections mentioned in Section 5 are therefore seen to have no validity.

As further evidence that the harmonic fields in the modulated condition are likely to have small effect on the operation of the machine, the no-load power/voltage curves for machine (*d*), for the two pole numbers, were taken and are shown in Fig. 10.

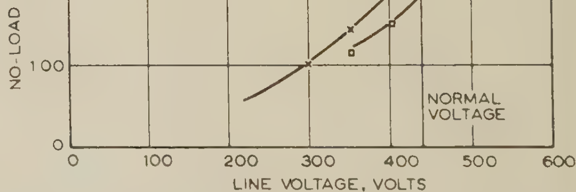

Fig. 10.—No-load power curves: Final 8/10 pole machine (*d*).
Windage deducted.
□ □ □ 8-pole parallel star.
× × × 10-pole series star.

It is thus clear that the no-load losses, which are governed by voltage and flux, are only marginally greater than when the machine is connected in a standard fashion. Stray losses on load are a function of current, and there is no reason to suppose—

either in theory or from tests—that these are, in any way, out of normal proportion in the modulated winding.

(7) FURTHER PROPOSED DEVELOPMENTS: CONCLUSION

At present, only speed and pole ratios having one of the values given, in general terms, in Section 2 can be achieved by these methods; and other industrially popular ratios, such as 4 : 3, cannot be directly obtained in this way. There is, however, good reason to believe that extensions of the method now under consideration may enable it in due course to be completely generalized. At least, it seems likely that the complete series of pole ratios 8/10 : 10/12 : 12/14 : 14/16, etc., will be successfully obtained; and a general but very simple method of obtaining small changes in the speed of induction motors would then be available.

The machines so far tested have had a constant (integral) number of coils per group, but there is reason to think that the use of certain fractional-slot windings, in some cases with unequally grouped coils, may effect a substantial improvement in performance for some types of duty. Again, it has been shown that for the machines discussed in this paper it is advantageous (although not necessary) to omit a quarter of the winding on modulation. The omission of portions of the winding often improves both the thermal conditions and the winding factor, besides reducing the undesirable harmonics; and it is actually necessary to omit certain coils when the initial pole number is not divisible by the desired difference in pole number, e.g. for 10/14 poles.

The authors intend to investigate in theory the conditions necessary for obtaining the best combination of all these possible advantages; and to carry out a series of experiments on machines wound to comply with the theoretical conclusions. They have thought it well, however, first to present in a short paper the basic principle of this new general method, in order to make it known to those interested in electrical machinery.

(8) ACKNOWLEDGMENTS

The authors are indebted to Messrs. R. L. Reeves and R. M. Collins, technicians in the Department of Electrical Engineering, University of Bristol, for their continued assistance in the winding of unusual electrical machines.

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(10) APPENDIX

Criterion for the Elimination of One of the Two Resultant Pole Numbers in Three-phase Amplitude-Modulated Field

Suppose that p is the original pole number, and that this is modulated to give resultant pole numbers $(p \pm 2k)$, where k is any integer. (In the case tested so far, k has been unity.)

Suppose that the original phase-spacing, with p poles, is $r(2\pi/3)$, where r can be any integer, other than 3 or a multiple of 3.

The phase spacing after modulating the pole number to $(p \pm 2k)$ will be

$$\frac{p \pm 2k}{p} r \frac{2\pi}{3}$$

To eliminate the higher or lower modulated pole number, respectively, the conditions are

$$\frac{p \pm 2k}{p} r \frac{2\pi}{3} = 2m\pi, \text{ where } m \text{ is any integer}$$

or

$$\frac{m}{r} = \frac{1}{3}\left(1 \pm \frac{2k}{p}\right), \text{ respectively.}$$

It now becomes necessary to determine if, and under what conditions, the phase spacing for the higher pole number is correct when the lower pole number is eliminated, and vice versa.

Now if

$$\frac{m}{r} = \frac{1}{3}\left(1 - \frac{2k}{p}\right)$$

which is the condition for the lower pole number to vanish, it follows that

$$1 + \frac{2k}{p} = 2 - \frac{3m}{r}$$

and the phase spacing for the higher modulated pole number, which is given by $\left(1 + \frac{2k}{p}\right) r \frac{2\pi}{3}$, will then be

$$(2r - 3m)\frac{2\pi}{3} = \lambda\frac{2\pi}{3}, \text{ say, where } \lambda \text{ is an integer.}$$

For correct spacing, λ cannot be 3 or a multiple of 3. Since r is never a multiple of 3, λ will not be so either, and this requirement is therefore met. When it is possible to comply with the condition for eliminating the lower pole number, the phase spacing for the higher pole number will thus simultaneously be made correct. Conversely, when it is possible to eliminate the higher pole number, the phases will always be correctly spaced for the lower pole number.

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Aug. 1958

THE EFFECT OF A VOLTAGE REGULATOR ON THE STEADY-STATE AND TRANSIENT STABILITY OF A SYNCHRONOUS GENERATOR

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(*The paper was first received* 21*st December*, 1957, *and in revised form* 11*th March*, 1958.)

SUMMARY

The paper is concerned with predicting the steady-state, dynamic and transient stability of a synchronous generator, with a voltage regulator, when coupled to an infinite busbar. The effect of the main regulator loop parameters, such as gain, exciter and main field time-constants, etc., on the stability of the system are examined. The solutions of the system equations are obtained by using an electronic analogue computer. The ideal stability boundary, for a particular system, is defined, and attempts are made, by the introduction of subsidiary feedback and series networks, to obtain the ideal characteristic. The influence of the transient reactance on stability and the effect of minimizing this parameter are considered. New voltage-excitation characteristics are described which have been found useful for predetermining power-angle curves for a machine with a voltage regulator and for checking computer solutions of steady-state stability boundaries.

LIST OF SYMBOLS

δ = Rotor angle.
f = Frequency, c/s.
H = Inertia constant, kWs/kVA.
P_i = Power input from prime mover.
P_u = Power output of synchronous generator.
$p\theta$ = Speed.
V_{fd} = Generator field voltage.
R_{fd} = Generator field resistance.
I_{fd} = Generator field current.
X_{fd} = Generator field reactance.
X_{ad} = Mutual reactance between generator field and direct-axis armature winding.
$V_I = I_{fd}X_{ad}p\theta$; a voltage proportional to I_{fd}.
$V_f = \dfrac{V_{fd}}{R_{fd}}X_{ad}p\theta$; open-circuit excitation voltage.
ϕ_{fd} = Field flux linkage.
$\Phi_{fd} = \phi_{fd}X_{ad}/X_{fd}$.
$\tau_{d0} = X_{fd}/R_{fd}$; generator field time-constant.
K_d = Damping-torque coefficient.
V = Bus voltage.
V_{dm}, V_d = Direct-axis machine terminal voltage and bus voltage, respectively.
V_{qm}, V_q = Quadrature-axis machine terminal voltage and bus voltage, respectively.
I_d, I_q = Direct-axis current and quadrature-axis current, respectively.
X_{dm}, X_{qm} = Machine direct- and quadrature-axis synchronous reactance, respectively.
X_t = Transmission-line series reactance.
X_d, X_q = Total direct- and quadrature-axis synchronous reactance, respectively.
X'_{dm} = Machine direct-axis transient reactance.
$V_Q = V - I_d(X_{dm} - X_{qm})$; voltage behind quadrature-axis reactance.
V_m = Machine terminal voltage.
V_r = Reference voltage.
V_s = Stabilizer output voltage.
μ = Regulator loop-amplification factor.
τ_s = Stabilizer time-constant.
τ_e = Exciter-field time-constant.
M = Mutual inductance of current transformer in field circuit.
V_x = Exciter field voltage.
μ_s = Stabilizer amplification factor.
A = Amplification factor in the positive feedback loop.

All voltages, currents, fluxes, reactances and resistances are expressed as per-unit quantities.

(1) INTRODUCTION

The predetermination of the stability of a synchronous generator with a voltage regulator, when connected in a power system, is conveniently divided into two categories, namely (*a*) steady-state and dynamic stability and (*b*) transient stability. It is known that an improvement in the steady-state stability limit may be effected by the use of a voltage regulator[1,2] in certain cases, but little is known concerning improvements in the transient stability limit. One possible approach to the steady-state stability problem is the use of small-oscillation theory,[3] which effectively eliminates non-linear terms in the equations. In the authors' view this method is of limited value in practice, since it requires lengthy and tedious computation, particularly if the problem is one of optimization of system parameters. The authors have also experienced difficulty in inserting initial conditions into small-oscillation equations, but the method may be useful in determining positive and negative damping torque coefficients as Park[3] originally intended. Analytically, dynamic and transient stability problems are difficult owing to the existence of non-linear terms in the system equations, and hence familiar criteria of stability are not readily applicable. Responses to transient disturbances are not easily computed. The availability of computers facilitates the solution of these stability problems, and although it is not clear whether digital or analogue types should be employed, the authors have found the analogue computer to be ideal for this work and for previous studies of synchronous-machine transient stability.[4,5] The rapid advance in automatic control-system techniques has raised the interesting point of what effects these techniques will have on stability when applied to synchronous machines in power systems.

The objects of the paper are therefore as follows:

(*a*) To show the possible improvements in steady-state and dynamic stability which result from the use of a voltage regulator and to indicate that optimum values of regulator loop parameters exist. In connection with this section of the work a new set of voltage-excitation characteristics has been developed for the predetermination of power-angle curves for a machine with a voltage regulator. These characteristics are useful in

Written contributions on papers published without being read at meetings are invited for consideration with a view to publication.

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themselves and are particularly helpful in facilitating the adjustment of initial conditions in the computer, and for checking some of the computations.

(*b*) To show the possible improvements in the transient stability limit of the generator and, in particular, the effects of subsidiary feedbacks and series corrective networks in the regulator loop additional to the main loop. These additional stabilizing devices include a phase-advance circuit, a transient negative feedback across part of the regulator loop and a positive feedback proportional to the rate of change of field current.

(2) THE SYSTEM STUDIED

The basic system, without subsidiary feedback, is shown diagrammatically in Fig. 1. It consists of a salient-pole syn-

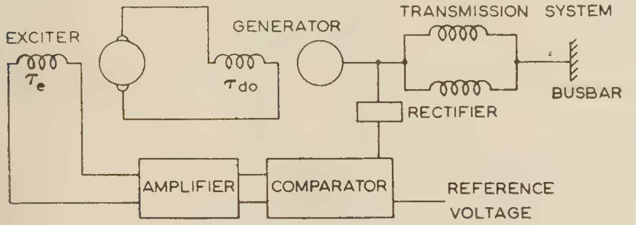

Fig. 1.—Basic system of synchronous machine and voltage regulator.

chronous generator connected to an infinite system via a double-circuit transmission line. The generator has a continuous fast-acting voltage regulator coupled to it which regulates the machine terminal voltage to a fixed value by controlling the excitation. The system is connected in the form of a closed loop and is actuated by the difference between the reference voltage and the machine terminal voltage. The system parameters are given in Table 1.

Table 1

$X_{dm} = 1$ per unit	$X'_{dm} = 0{\cdot}2$ per unit
$X_{qm} = 0{\cdot}7$ per unit	$H = 5$ kWs/kVA
$X_t = 0{\cdot}5$ per unit	$V = 1$ per unit

For this salient-pole machine it is assumed that damper windings are not present, and consequently K_d is zero.

The remaining parameters such as loop gain μ, exciter-field and main-field time-constants, τ_e and τ_{d0}, respectively, are considered to be variables, except when otherwise stated. This also applies to the parameters of additional feedback circuits, and they are defined when these circuits are introduced. In practice, it is necessary to restrict the synchronous-machine field voltage and exciter field voltage to within safe limits. The circuit used to simulate voltage limiting is given in Section 3. The positive and negative limits placed on V_x are $+3{\cdot}5$ and -1 per unit, respectively.

(3) SYSTEM EQUATIONS AND EQUIVALENT ANALOGUE INTERCONNECTIONS

The equations of the synchronous machine and transmission system are derived from Park's equations[6] and are contained and derived in a previous paper.[4] The following assumptions are made:

(*a*) The machine is ideal.

(i) The armature flux wave is sinusoidally distributed in space.
(ii) The saturation is negligible.
(iii) The effect of eddy-current and hysteresis loss is negligible.

(*b*) The percentage speed change during a transient which results in the machine remaining in synchronism is negligible, i.e. the component of generated voltage due to the rate of change of rotor angle is negligible compared with the voltage generated at fundamental speed.

(*c*) The voltages induced in the armature by the rate of change of armature flux linkage are negligible compared with the voltages generated by the fluxes rotating at fundamental speed.

(*d*) The armature and line resistances are negligible.

(*e*) The action of the prime-mover governor is not sufficiently fast to change the mechanical power input following a small change in speed, so that the mechanical power input is constant.

The equations of the voltage regulator are obtained from a simplified block schematic of the physical system. The complete set of equations is as follows:

Equation of motion

$$P_i = \frac{H}{180f}\frac{d^2\delta}{dt^2} + P_u \quad . \quad . \quad . \quad . \quad . \quad (1)$$

Power output $$P_u = V_Q I_q \quad . \quad . \quad . \quad . \quad . \quad . \quad (2)$$

Derived quantity (voltage behind quadrature-axis reactance)

$$V_Q = V_I - (X_{dm} - X_{qm})I_d \quad . \quad . \quad . \quad . \quad (3)$$

Field $$\Phi_{fd} = \frac{V_f - V_I}{\tau_{d0}p} \quad . \quad . \quad . \quad . \quad . \quad (4)$$

$$\Phi_{fd} = V_I - (X_{dm} - X'_{dm})I_d \quad . \quad . \quad . \quad (5)$$

Components of busbar voltage

$$V_q = V\cos\delta = V_{qm} - X_t I_d \quad . \quad . \quad . \quad (6)$$

$$V_d = V\sin\delta = V_{dm} + X_t I_q \quad . \quad . \quad . \quad (7)$$

Components of machine terminal voltage

$$V_{qm} = V_I - X_{dm}I_d \quad . \quad . \quad . \quad . \quad . \quad (8)$$

$$V_{dm} = X_{qm}I_q \quad . \quad . \quad . \quad . \quad . \quad . \quad (9)$$

Terminal voltage $$V_m = \sqrt{(V_{dm}^2 + V_{qm}^2)} \quad . \quad . \quad . \quad . \quad (10)$$

Exciter field voltage $$V_x = \mu(V_r - V_m) \quad . \quad . \quad . \quad . \quad (11)$$

where $$V_{x\,max} \geqslant V_x \geqslant V_{x\,min} \quad . \quad . \quad . \quad . \quad (12)$$

$$V_f = \frac{V_x}{1 + \tau_e p} \quad . \quad . \quad . \quad . \quad . \quad . \quad (13)$$

The equivalent analogue interconnection for eqns. (1)–(13) is given in Fig. 2. The amplifiers, integrators, function generators and multiplier are of conventional design. The vector addition unit consists of a high-gain amplifier with diode function-squaring circuits in the input and feedback paths, as shown in Fig. 3. The circuit for the limiter is shown in Fig. 4, and although perfect limiting does not occur with this circuit, the maximum error is about 1%, as indicated by the dotted lines. When the effect of a stabilizing transformer is considered, it is represented in the analogue by the circuit shown in Fig. 5. The equation of a stabilizing transformer is

$$V_s = \frac{\mu_s \tau_s p V_f}{1 + \tau_s p} \quad . \quad . \quad . \quad . \quad . \quad . \quad (14)$$

A transfer function of the form of eqn. (14) may be simulated by placing a series *RC* network at the input of a high-gain amplifier which also has resistive feedback. This simple circuit is elaborated in Fig. 5 to facilitate variation of stabilizer gain and time-constant. It is necessary to include an initial-condition relay, in order to ensure that the stabilizer output voltage is

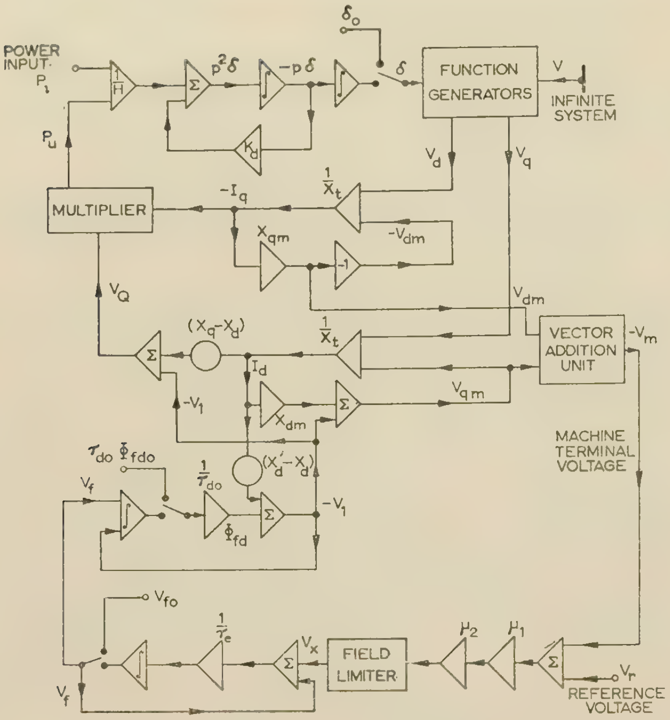

Fig. 2.—Equivalent analogue of the basic system.

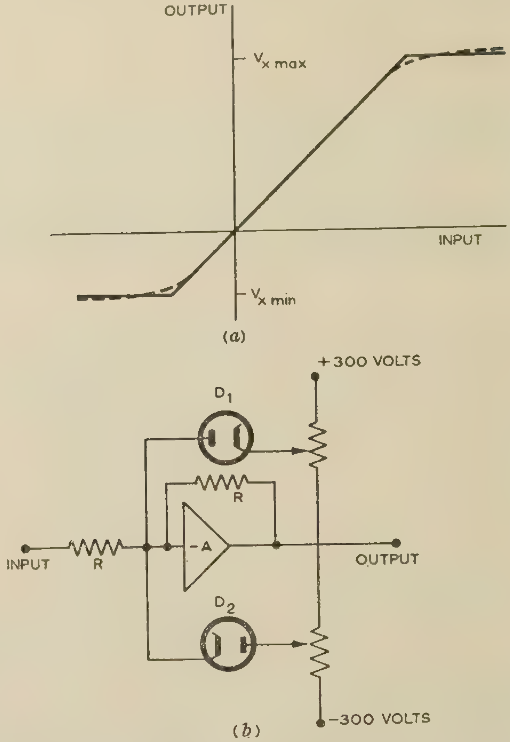

Fig. 4.—Voltage limiter.

(a) Characteristic.
(b) Analogue circuit.

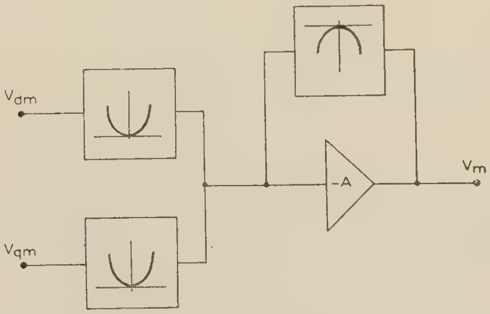

Fig. 3.—Vector addition unit.

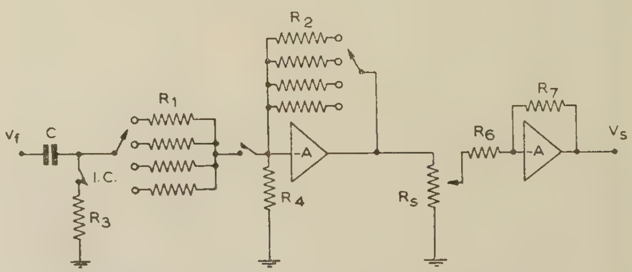

Fig. 5.—Stabilizing-transformer analogue.

zero in the 'initial-condition' state, and also a 'hold' relay to interrupt computation if necessary. The computer is arranged to operate in any one of four states, namely 'compute', 'set initial conditions', 'interrupt computation' and 'set zero'. In the initial-condition state all integrators are inoperative, this condition corresponding to absolute steady-state operation of the system. Although facilities are available for computing with a time-scale factor of less than unity, all computations are carried out in real time.

(4) STEADY-STATE AND DYNAMIC STABILITY

(4.1) Factors affecting Stability

The steady-state stability limit of a synchronous generator is the maximum power that the machine can supply without falling out of synchronism. The stability limit is a function of field excitation, rotor angle and system parameters. For fixed excitation there is a corresponding constant value for the power limit of the machine.

By the use of a continuously-acting voltage regulator, usually regulating the terminal voltage to some fixed reference by controlling the excitation, the generator may be loaded beyond its normal steady-state limit. The excitation is now a function of terminal voltage and regulator parameters. In this condition the load angle may advance beyond its normal (i.e. with fixed excitation) maximum value, and the generator is then operating in the dynamic stability region. Under these conditions, the stability of the system depends upon the correct functioning of the regulator, and as such, the system is sometimes said to be statically unstable. As the dynamic stability limit of the machine is approached, self-excited oscillations occur, i.e. the machine hunts. These oscillations may increase in amplitude and eventually cause the machine to become unstable. Dynamic stability is achieved by rapid response of the regulator and exciter and the large inertia of the rotor, which restricts the rate of change of rotor angle. This implies that the ideal arrangement is a regulator and exciter with large gain and no time lags in the loop. This has been shown to be true.

With a finite generator-field time-constant, however, a finite exciter-field time-constant is beneficial in that it introduces a certain amount of damping, and the degree to which oscillations persist (and, in fact, originate) in the system depends on the

amount of damping present in the machine. Thus an optimum value of τ_e may be selected. Furthermore, when lags are present in the loop the loop gain is critical, and the choice of too small a value results in an inefficient regulator. On the other hand, a very high gain introduces loop instability, resulting in a much lower dynamic stability limit.

(4.2) Voltage-Excitation Characteristics

The voltage-excitation characteristics of a synchronous generator may be plotted in several different ways. The most common method is to plot the generator terminal voltage as a function of its field excitation voltage, or field current, for fixed power factors. In plotting these curves the armature current must be specified. It is felt that this limits their usefulness in presenting an overall picture of the operation of a generator under varying load conditions and changing armature currents. For this reason a new set of voltage-excitation curves has been derived, as shown in Fig. 6, for a synchronous generator,

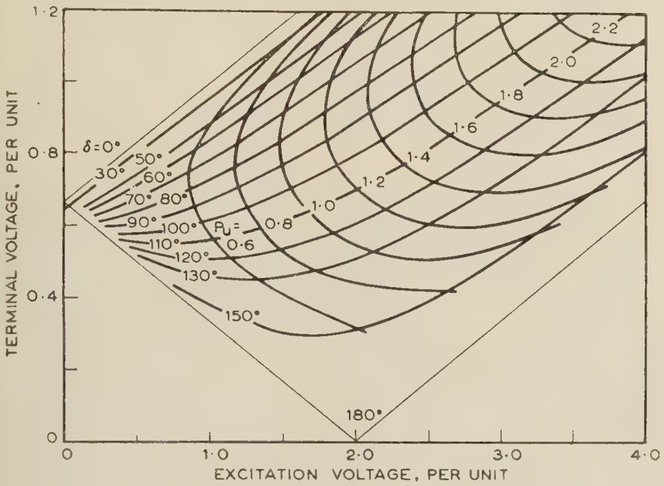

Fig. 6.—Voltage-excitation characteristics.

the parameters of which are given in Table 1. On these curves the terminal voltage is plotted as a function of the excitation voltage for (*a*) constant power outputs and (*b*) constant rotor angles. The equations from which these curves are derived are as follows:

$$P_u = \frac{V_I V \sin\delta}{X_d} + \frac{V^2(X_d - X_q)}{2X_d X_q}\sin 2\delta \quad . \quad . \quad (15)$$

$$V_{dm} = V_d\frac{X_{qm}}{X_q} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (16)$$

$$V_{qm} = \frac{V_I X_t + V_q X_{dm}}{X_d} \quad . \quad . \quad . \quad . \quad . \quad . \quad (17)$$

$$V_m = (V_{dm}^2 + V_{qm}^2)^{1/2} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (18)$$

The advantage of this method is that the power-angle curve of a generator may be obtained easily for any initial terminal voltage and power output. This applies to a machine with either manual or voltage-regulator-controlled excitation, the characteristics being particularly useful for the latter case. The initial power output and terminal voltage define a point on the voltage-excitation characteristic. For fixed excitation a line is drawn through this point parallel to the terminal-voltage axis. The generator is constrained to operate on this line, and hence the variation of power output with rotor angle may be determined from the points of intersection.

To obtain power-angle curves for a machine with a voltage regulator the method is similar and is as follows:

The equation of the regulator in the steady state is

$$V_f = \mu(V_r - V_m) \quad . \quad . \quad . \quad . \quad (19)$$

Differentiating with respect to V_f gives

$$\frac{dV_m}{dV_f} = -\frac{1}{\mu} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (20)$$

The initial-condition point defined by the initial power and terminal voltage is again selected, but now a line of slope $-1/\mu$ is drawn through this point. It can be seen from eqn. (19) that this line will cut the terminal voltage axis at the required reference voltage. Again the generator is constrained to operate on this line, and the power-angle curve may be derived from the points of intersection. The power-angle curve obtained by this method is accurate over its complete range if there are no lags in the regulator loop. If lags are present, the curve is accurate provided that the generator is not operating in the dynamic stability region. For dynamic and transient stability the solutions are obtained using the analogue computer, as described in Sections 5 and 6. The authors have found these voltage-excitation characteristics most useful for checking computer solutions in the steady-state stability regions.

(5) EFFECT OF VOLTAGE REGULATOR ON STEADY-STATE AND DYNAMIC STABILITY

For the generator defined in Table 1 the initial operating conditions were chosen to be 1 p.u. for both the power input and the terminal voltage, corresponding to rated terminal voltage and rated power output.

With the computer in the initial-condition state the desired operating conditions are set in by adjusting rotor angle and field flux linkage. The voltage proportional to the field current, i.e. V_I, is recorded. From an inspection of Fig. 2 or the equations of the system it can be seen that in the steady state V_f and V_x must both be equal to V_I. Since V_f is an initial-condition voltage on an integrator in the steady state it can be set in easily. The voltage V_x is set, after the regulator amplification has been selected, by adjusting the reference voltage. With V_f and V_x both adjusted to be equal to V_I, computation is begun. If the system is stable the power input is slowly increased until the point of maximum power output occurs. At this point any further increase in power input causes the system to become unstable. The variables in this test are regulator gain and generator- and exciter-field time-constants. The results are recorded in Fig. 7 and compared with the unregulated power limit.

Curve (*a*) shows the effect of regulator amplification on the power limit of the system with no lags in the exciter or generator. This corresponds to a perfect regulator, except that the gain is finite, since the power limit obtained for a given regulator amplification is at its maximum. A check on this result was obtained by plotting the same curve using the voltage-excitation characteristics referred to in the previous Section.

Comparison of curves (*b*), (*c*) and (*d*) shows that for a fixed value of τ_{d0}, variations in τ_e have no effect on the power limit of the system for small amplification factors. The point at which each curve leaves the common curve is determined by τ_e. The sharp discontinuity which occurs at each of these points is caused by the onset of self-excited oscillations in the system. If no field limiting were present the power limit of the system would fall very rapidly as the regulator amplification was increased beyond the value at which the discontinuity occurs. The effect of the limiter in the region where hunting occurs is

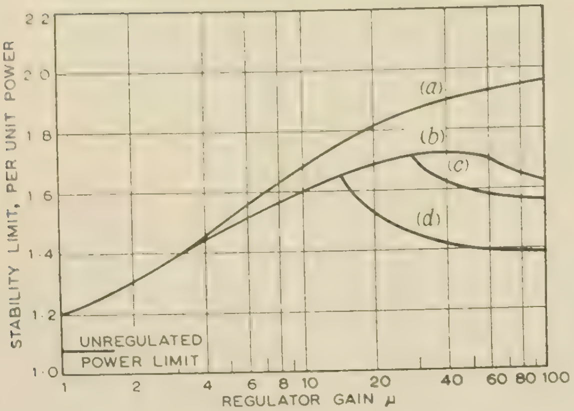

Fig. 7.—Steady-state stability boundaries.

(a) $\tau_e = 0$, $\tau_{d0} = 0$. (b) $\tau_e = 2$, $\tau_{d0} = 5$.
(c) $\tau_e = 1$, $\tau_{d0} = 5$. (d) $\tau_e = 0 \cdot 5$, $\tau_{d0} = 5$.

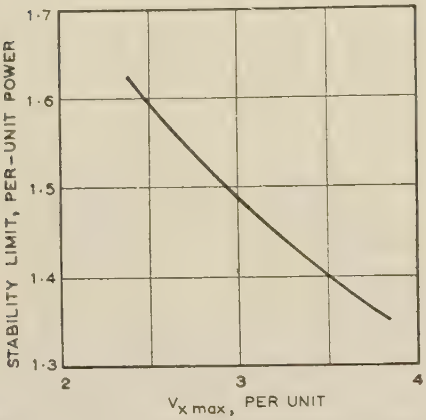

Fig. 8.—Effect of limiter on steady-state stability.

$\tau_{d0} = 5$, $\tau_e = 0 \cdot 5$, $\mu = 50$,
$V_{x,min} = -1 \cdot 0$. No stabilizer.

shown in Fig. 8, where it becomes apparent that the limiter reduces the severity of the self-excited oscillations, thus causing the power limit of the system to be constant for larger values of regulator amplification. However, in view of these oscillations, it is clearly an unsuitable region for operation.

The effect of the exciter- and generator-field time-constants on the stability of the system is demonstrated with greater clarity in Fig. 9. The regulator gain is fixed at 40, and the curves show the variations of the steady-state stability limit with τ_e for two fixed values of τ_{d0}. These curves show that, for a maximum

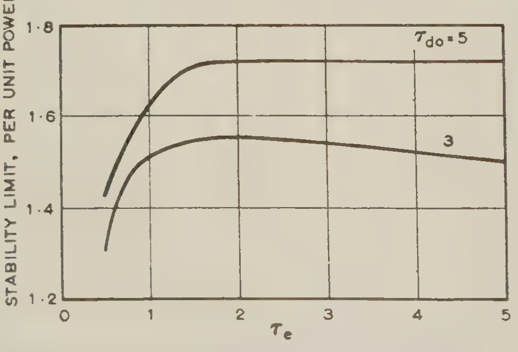

Fig. 9.—Effect of exciter field time-constant on steady-state stability.

$\mu = 40$. No stabilizer.

stability limit, there may or may not be an optimum exciter-field time-constant, but in either case there is certainly a minimum value.

In order to counteract the self-excited oscillations, a circuit simulating a stabilizing transformer was introduced into the regulator loop section of the computer, and its effect on curves (*b*) and (*d*) of Fig. 7 was investigated. In this stabilizing circuit (Fig. 5) gain and time-constant are both variable. The curves of Fig. 10 demonstrate that the stabilizer has the desired effect.

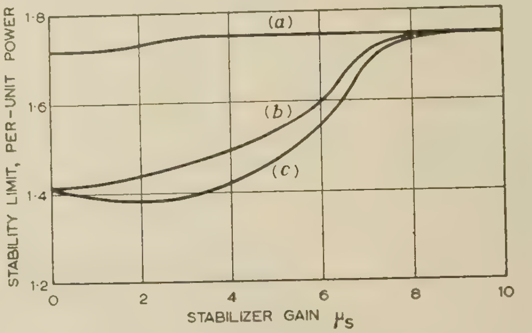

Fig. 10.—Effect of stabilizing transformer on steady-state stability.

$\tau_{d0} = 5$, $\mu = 40$
(a) $\tau_s = 2$ $\tau_e = 2$
(b) $\tau_s = 0 \cdot 25$ $\tau_e = 0 \cdot 5$
(c) $\tau_s = 2$ $\tau_e = 0 \cdot 5$

It can be seen that, provided that the stabilizer gain is sufficiently large to enable operation on the upper flat portion of the curve to be possible, the stabilizer time-constant is not critical.

To sum up the results of this Section it may be said that, for a regulated synchronous machine, there are several combinations of the various time-constants and amplification factors in the regulator loop which will allow the generator to operate at what appears (at this stage) to be its maximum power-limit (i.e. 1·75 per unit power) with this type of regulator. Provided that certain minimum values of some of these loop parameters are exceeded, the choice of their actual values is not very critical.

The reservation 'at this stage' is included because reference to steady-state stability limits is again made in Section 6 as a result of observation of other feedbacks on transient stability.

(6) EFFECT OF VOLTAGE REGULATOR ON TRANSIENT STABILITY

(6.1) Without Subsidiary Feedback

For the investigation of the transient stability of a generator and regulating device several tests may be applied, but all are concerned with the behaviour of the system subsequent to a sudden disturbance. The latter may be a fault on a transmission line, the dropping of a line section, a sudden application of load or a change in power input.

The change in power input is the method used in the tests described below. The generator is adjusted initially to operate at rated power output and terminal voltage and is then subjected to a sudden increase in the power input. Transient stability is said to exist if the machine regains a state of equilibrium after such a disturbance. The generator is subjected to gradually increasing power impulses until the transient stability limit is reached and synchronism is lost.

The initial conditions of rated power output and terminal voltage are maintained constant for all tests. The effects of changes in τ_{d0}, τ_e and regulator gain on the transient stability limit are observed.

The stability limit is plotted as a function of regulator gain for several different combinations of time-constants in Fig. 11

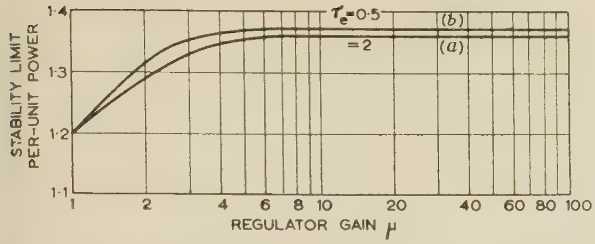

Fig. 11.—Effect of exciter time-constant on transient stability.
$\tau_{d0} = 5$. No stabilizer.

Let us consider curve (*a*). This shows that the transient stability limit is almost constant for regulator gains greater than five. Curve (*b*) shows the effect of decreasing the exciter-field time-constant; the power limit is increased slightly owing to the faster build-up rate of the exciter.

During the course of subsequent tests it was observed that it was not possible to raise the transient power limit by any substantial amount above that shown by the flat region of Fig. 11, irrespective of the values of τ_{d0} and τ_e. This was, at first, thought to be due to the presence of the limiter in the loop, but further tests revealed that this is not true, since variation of the ceiling voltage had no effect on the transient stability limit of the system.

It is concluded from these observations that the regulating system in its present form is too slow, and as a result is unable to deal with any large transient disturbances. We must assume, therefore, that if any noteworthy increase in the transient stability limit of the generator is to be achieved, it must arise from the use of subsidiary feedback and networks in addition to the main regulating loop.

During a transient the synchronizing torque generated in the machine is approximately inversely proportional to the transient reactance. Consequently the stability limit is increased as the transient reactance is reduced. To verify this statement the

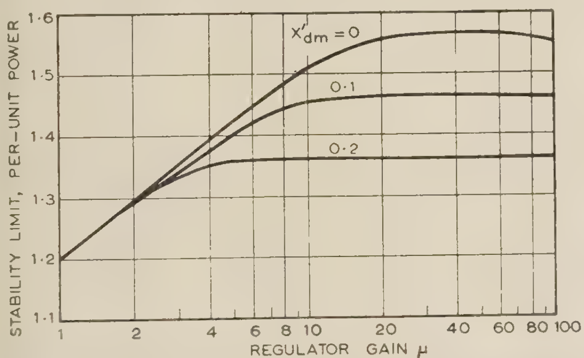

Fig. 12.—Effect of transient reactance on transient stability.
$\tau_{d0} = 5$, $\tau_e = 2$. No stabilizer.

transient stability limit of the system is plotted in Fig. 12 as a function of μ for three different values of the transient reactance.

It can be argued that the effect of a voltage regulator, by virtue of the fact that it tries to maintain the terminal voltage of the machine at a fixed value, is to reduce the synchronous reactance during steady-state operation. In so doing, the steady-state stability limit is improved.

If by a subsidiary feedback the effective reactance could be reduced during a disturbance, an improvement in the transient stability limit would be possible. Kron[7] has suggested that a positive feedback proportional to the rate of change of field current can cancel the transient reactance. The following analysis confirms this view.

Consider two coils representing the field and direct-axis armature circuits of a synchronous generator, the direct-axis circuit being short-circuited as shown in Fig. 13. In addition

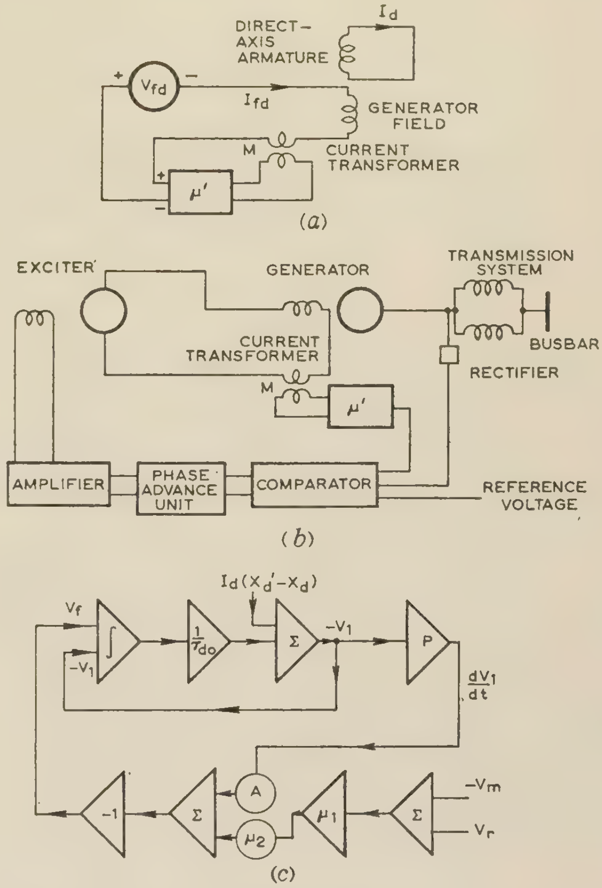

Fig. 13.—Introduction of positive feedback proportional to the rate of change of field current.

(*a*) Simplified circuit with positive feedback.
(*b*) Complete system including positive feedback.
(*c*) Analogue modification to incorporate positive feedback.

to the normal field voltage V_{fd}, a voltage equal to $+MdI_{fd}/dt$ (obtained at the secondary of a current transformer) is amplified by a factor μ' and introduced in series aiding with V_{fd}. In the analysis of transient reactance it is unnecessary to consider rotation; both coils are therefore assumed to be stationary. The equations for the circuit of Fig. 13 are as follows:

$$V_{fd} + M\mu' pI_{fd} = Z_{fd}I_{fd} - X_{ad}pI_d \quad (21)$$

$$0 = X_{ad}pI_{fd} - Z_{dm}I_d \quad (22)$$

where

$$Z_{fd} = R_{fd} + X_{fd}p \quad (23)$$

$$Z_{dm} = R_{dm} + X_{dm}p \quad (24)$$

Eliminating the field current gives

$$0 = X_{ad}pV_{fd} + X_{ad}^2p^2I_d - Z_{dm}I_dZ_{fd} + Z_{dm}M\mu' pI_d \quad (25)$$

If we divide throughout by $X_{fd}p$ and express Z_{dm} and Z_{fd} in operational form, we get

$$0 = \frac{X_{ad}}{X_{fd}}V_{fd} - \frac{R_{fd}R_{dm}(1 + \tau_{d0}p)(1 + \tau_{dm}p)I_d}{X_{fd}p} + \frac{X_{ad}^2pI_d}{X_{fd}} + \frac{M\mu' R_{dm}}{X_{fd}}(1 + \tau_{dm}p)I_d \quad (26)$$

If it is assumed that the field and direct-axis armature time-constants are large compared with unity (which is not unreasonable), eqn. (26) reduces to

$$0 = \frac{X_{ad}}{X_{fd}} V_{fd} - \left[\left(X_{dm} - \frac{X_{ad}^2}{X_{fd}}\right) - \frac{M\mu' X_{dm}}{X_{fd}}\right] pI_d \qquad (27)$$

The quantity $(X_{dm} - X_{ad}^2/X_{fd})$ is the normal expression for transient reactance X'_{dm}, and therefore from eqn. (27) it can be seen that the effective transient reactance is reduced by an amount equal to $M\mu' X_{dm}/X_{fd}$. If $M\mu' X_{dm}/X_{fd}$ is adjusted to be equal to $X_{dm} - X_{ad}^2/X_{fd}$, then effectively the transient reactance is cancelled. The practical significance of this is now demonstrated.

(6.2) Effect of Positive Feedback proportional to Rate of Change of Field Current

In an actual system the method outlined above may be incorporated by modifying the regulator loop to that shown in Fig. 13(*b*). The regulating signal has been modified and now contains a voltage proportional to the rate of change of field current. In order that this signal shall not be interfered with before it reaches the field of the generator, a phase-advance unit is included to cancel the lag in the exciter field.

In the computer the cancellation of exciter lag is simulated by removing the exciter analogue. The field-limiting device was also removed, since it was felt that a true assessment of the effect of this positive feedback loop could only be obtained if there were no limits. The modified analogue of the regulator loop is shown in Fig. 13(*c*). In this, changes in the main regulator amplification factor do not affect the amount of positive feedback, as such changes would in the case of the actual circuit of Fig. 13(*b*). In the analogue the scaling factor A, in the rate-of-change-of-field-current loop, is related to the parameters by the equation

$$A = \frac{M\mu'\mu_r}{R_{fd}} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (28)$$

where μ_r is the overall gain of the phase-advance unit and the main amplifier.

As previously stated, the object of the subsidiary feedback is to increase the transient stability limit of the system, but it is also found to have a marked effect on its steady-state stability. In Fig. 14 a curve is shown which indicates how the steady-state

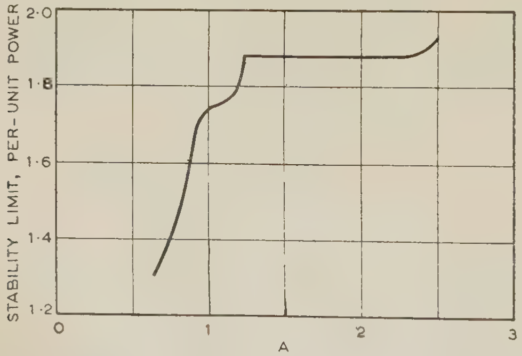

Fig. 14.—Effect of positive feedback on steady-state stability.

$\mu_r = 40, \tau_e = 0, \tau_{do} = 5$
No stabilizer and no limiter.

stability is affected by various amounts of feedback. The initial conditions are the same as in previous tests and the overall regulator gain is 40. Comparison of this curve with the results shown in Fig. 7 reveals that the flat part of the curve represents the ultimate steady-state limit of the system for the particular regulator gain selected for this test. For small values of A the system is liable to hunt continuously and is therefore not a very suitable region for operation. On the flat part of the curve the system does not hunt and appears to be an ideal region for operation. Further increase in A causes violent oscillation in the loop. The conditions at which these oscillations begin are found to be almost the same as those required for the complete cancellation of the transient reactance. These conditions are given in Section 6.1. This was verified in a series of tests involving various values of τ_{d0} and X'_d. The onset of these oscillations was also found to be practically independent of the operating point of the machine and also the gain in the regulator loop.

When the generator is subjected to transient tests of the same type as previously used, a very marked increase in the transient stability limit of the system is noted, as indicated in Fig. 15.

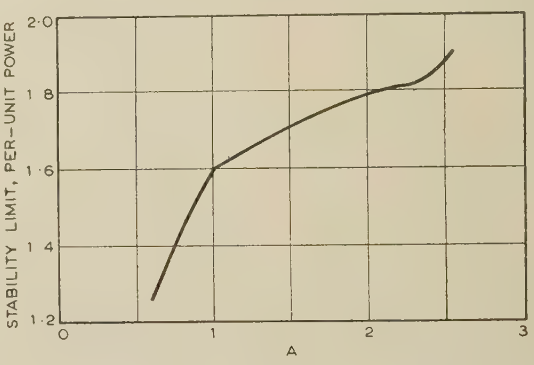

Fig. 15.—Effect of positive feedback on transient stability.

$\mu_r = 40, \tau_e = 0, \tau_{do} = 5$
No stabilizer and no limiter.

Here, the reasons for the rapid decline in stability limit for small values of A and the comparatively rapid increase for large values of A are the same as those given for the steady-state case. The central region of Fig. 15 is again eminently suitable for operation. It is also found that the higher the value of A the greater is the damping present, and just before the condition for violent oscillation is reached, the system is almost critically damped.

A better assessment of the effect of the positive-feedback loop is perhaps obtained by examination of Fig. 16. Here two values of A have been selected and the steady-state and transient stability limits are plotted as functions of the overall regulator

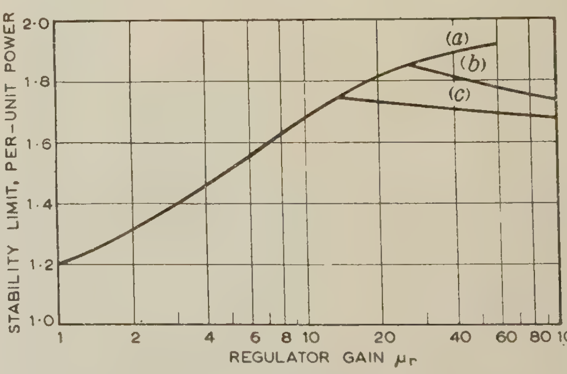

Fig. 16.—Comparison of steady-state and transient stability boundaries.

$\tau_e = 0, \tau_{do} = 5$. No stabilizer and no limiter.
(*a*) Steady-state limit for $A = 1{\cdot}5$ or $2{\cdot}2$.
(*b*) Transient limit for $A = 2{\cdot}2$.
(*c*) Transient limit for $A = 1{\cdot}5$.

gain. The steady-state curve, i.e. curve (*a*), is identical to that given in Fig. 7 for the ideal regulator. The transient-stability-limit curve also follows this ideal characteristic up to the break points. Comparison of the curves of Fig. 16 with those of Fig. 11 clearly emphasizes the marked improvement that this type of feedback has on the transient stability.

(7) CONCLUSIONS

The results of the experiments show clearly that a continuous fast-acting voltage regulator, in conjunction with subsidiary feedback, can considerably influence the steady-state, dynamic and transient stability of a synchronous generator connected in a power system. Certain assumptions have been made concerning the equations of synchronous machines, and the results should be considered in relation to these assumptions. The assumptions (*a*) that voltages induced by the rate of change of armature flux-linkages are negligible, (*b*) that the effect of speed variation is negligible and (*c*) that armature and transmission line resistances are negligible, are considered by the authors to be quite valid for the majority of machines. The effect of saturation requires further investigation.

The question of damping in the machine requires comment. In the experiments a salient-pole machine without damper windings has been investigated. Any damping due to the field winding is included in the analysis. The addition of damper windings may or may not have an effect on stability when a voltage regulator is used, since it appears that damping created by voltage-regulator action and subsidiary feedbacks may well outweigh any damping introduced by amortisseur windings. This effect is particularly noticeable when the positive-feedback loop is added, as a result of which the system becomes almost critically damped.

The method used by the authors for assessing transient stability may appear to be unusual. It was adopted in order to eliminate a number of variables (e.g. type, position, duration of fault and reclosing) associated with the more normal procedure of applying short-circuits. It is felt that this method also gives a clearer indication of the relative stability of the system.

To summarize the more important result, it can be said that the positive feedback proportional to the rate of change of field current, in conjunction with the phase-advance circuit, enables the steady-state stability boundary to coincide with the ideal characteristic throughout the complete range of regulator gain. This is also true for the transient stability boundary over part of the range of regulator gain, but in this case an optimum value of gain exists.

(8) ACKNOWLEDGMENTS

The authors are grateful to Prof. J. M. Meek for his interest in the work and for facilities provided in the Electrical Engineering Laboratories of Liverpool University.

They also wish to thank the Central Electricity Generating Board for financial support.

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DESIGN OF FRACTIONAL-SLOT WINDINGS

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SUMMARY

A theory is developed by which the electrical design of a standard fractional-slot winding may be readily established. Formulae are given for the calculation of the amplitude of the fundamental and the various harmonics, and three methods are developed for reducing the amplitude of any particular harmonic.

The calculation of the noise produced by the vibration of the core set up by the lowest sub-harmonics is briefly discussed. A worked example is given which shows, *inter alia*, a practical method of designing suitable end connections for fractional-slot wave windings.

LIST OF PRINCIPAL SYMBOLS

A = Amplitude factor of flux-density wave.
a = Radius of core at bottom of slots.
b = Outside radius of stator core.
$c = (b/a)^2$.
$D = 2R, = a + b$.
E = Young's modulus.
f = Natural frequency of vibration of core.
F = Highest common factor of N_T and P_T = Number of sections in winding.
g = Acceleration due to gravity.
$I = (b - a)^3/12$.
K_d, K_{de}, K_{do} = Spread factors.
$K_p, K_{p1}, K_{p2} \ldots$ = Pitch factors.
L = Length of stator core.
$m = n_1 - n_2$.
$N = N_T/F$.
N_T = Number of slots.
n = Order of harmonics.
n_1, n_2 = Number of conductors under equivalent positive and negative poles, respectively.
$P = P_T/F$.
P_T = Number of pairs of poles.
$\mathscr{P}$ = Force produced by flux-density wave.
$R = (a + b)/2$.
S_1 = Number of slots in winding pitch.
u = Radial deflection of core by flux-density wave.
$\mu_1 = 0{\cdot}282(b - a)$, lb/in^2.
σ = Poisson's ratio.

(1) INTRODUCTION

The almost universal use of fractional-slot windings in medium and large 3-phase synchronous machines is due to the well-known advantages they offer from the point of view of manufacture and operating characteristics.[1] Their theory was apparently first published by Quentin Graham[2] and subsequently extended by J. F. Calvert,[3] M. G. Malti and F. Herzog,[4] M. M. Liwschitz[5,6,7] and M. Liwschitz-Garik.[8]

Although the contributions by these authors provide a basis for the design of fractional-slot windings, their theoretical approach and practical applications lack simplicity and rigour. The paper develops a simple analytical theory, in which the concept of the equivalent slot pitch is based on an unpublished report by B. Adkins and N. Kerruish, and which is capable of ready application to the design of standard fractional-slot windings. The theory covers methods of rearranging standard windings in order to reduce undesirable space harmonics, particularly those liable to cause noise and vibration, and simple equations are derived for calculating the amplitude factors of the fundamental and various harmonics. This theory takes no cognisance of whether the windings are connected in lap or wave, since, in the words of Professor E. B. Moullin,[9] 'the end connections concern us only in that they are necessary adjuncts to permit the current to flow in the desired directions'.

The connections are a simple matter in the case of lap windings but may be complicated when wave windings are used. Since the latter are assuming increasing importance in the design of large hydro-electric generators a discussion is included of methods of designing the connections of both standard and modified fractional-slot wave windings.

(2) CONDITIONS FOR BALANCE IN 3-PHASE FRACTIONAL-SLOT WINDINGS

A fractional-slot winding is one in which the ratio of the number of slots to the number of pairs of poles, reduced to its lowest terms, is not an integer.

To ensure balance of the three phases certain conditions, common to all 3-phase windings whether integral or fractional, must be satisfied. The number of slots N_T must be a multiple of three but need not be a multiple of the number of pairs of poles P_T. If N_T and P_T have a highest common factor F the machine can be divided into F identical sections, each section having $N = N_T/F$ slots and $P = P_T/F$ pairs of poles. Thus it is only necessary to consider one section with N slots and P pairs of poles.

In any one section, N must be a multiple of 3 and P must not be a multiple of 3. It is also necessary that if any conductor belongs to phase I, the conductor $N/3$ slots away in the same layer must belong to phase II and that $2N/3$ slots away must belong to phase III. In either layer each phase occupies $N/3$ slots, and the grouping of the conductors in all three phases must be exactly the same, but displaced $N/3$ and $2N/3$ slots respectively.

If all coils have the same pitch, S_1 slots, the top layer has the same grouping as the bottom layer but displaced from it by S_1 slots, the conductor electromotive forces being reversed in direction. In designing a winding it is thus only necessary to consider the arrangement of conductors in one phase, and usually only in one layer.

(3) THE STANDARD FRACTIONAL-SLOT WINDING

In integral slot windings it is normal practice[1] to arrange the conductors in 60° phase bands so that under each pole there are three equal groups of conductors, each group corresponding to a phase band.

In a fractional-slot winding there will usually be three groups

Written contributions on papers published without being read at meetings are invited for consideration with a view to publication.
Dr. Walker and Mr. Kerruish are with the British Thomson-Houston Co., Ltd.

of conductors under each pole, each group differing more or less from 60° in width and also differing similarly from the width of the groups under the other poles. It is thus necessary to arrange the groups of a given phase under the various poles so that all the conductors under, e.g., the positive poles are uniformly located within a 60° *equivalent* phase band under one *equivalent* positive pole, and that all the conductors under the negative poles are uniformly located within an *equivalent* 60° phase band under one *equivalent* negative pole; the positive and negative phase bands must of course be 180° out of phase. Such an arrangement of conductors, which, as in the case of integral-slot windings, gives the maximum value of the spread factor K_d of the fundamental wave and thus the best utilization, will be designated the 'standard' winding. Any variations from this winding, whatever favourable effects such variations may have on harmonics, will always reduce the value of K_d for the fundamental (see Section 12.1).

If the numbers of conductors under the equivalent positive and negative poles are represented by n_1 and n_2 respectively, where $(n_1 + n_2) = N/3$, and if $N/3$ is even, a condition for obtaining the maximum value of K_d for the fundamental is that $n_1 - n_2 = 0$. This makes it possible to arrange 60° phase bands on the equivalent positive and negative phase bands.

If $N/3$ is odd, n_1 cannot equal n_2 and the maximum value of K_d is obtained when $n_1 - n_2 = 1$. Here the width of the two phase bands will be slightly greater and slightly less than 60° but the sum of the angles will be 120°.

With given values of N, P and $n_1 - n_2$ it is now necessary to determine the arrangement of the groups of conductors, in a given phase, under the various poles to ensure that all the conductors are uniformly located in the two equivalent phase bands. 'Uniformly located' implies that the electrical angles of the successive conductors in an equivalent phase band form an arithmetical progression.

To determine this arrangement, let

$$\frac{N}{P} = Y \pm \frac{q}{P} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (1)$$

where Y and q are integers, Y being the nearest integer to N/P. If $q = 1$, $Y = X$, the slot pitches on the machine corresponding to one slot pitch under the equivalent pole. If $q \neq 1$, eqn. (1) is multiplied throughout by x, thus

$$\frac{xN}{P} = xY \pm \frac{xq}{P} \quad . \quad . \quad . \quad . \quad . \quad . \quad (2)$$

where x is obtained from the relationship

$$P - qx = \pm 1 \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (3)$$

Eqn. (2) can now be written

$$\frac{xN}{P} = X \pm \frac{1}{P} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (4)$$

where X is the nearest integer to xN/P.

It is now possible, starting at conductor 1, to step forward X conductors at a time so as to include in turn all N conductors. The total number of conductors traversed is XN, i.e. X times round the circumference. Now the electrical angle between two adjacent slots is

$$2\theta_1 = \frac{(360n)}{N} \text{ degrees} \quad . \quad . \quad . \quad . \quad . \quad (5)$$

where n represents the order of any harmonic and can assume any integral value. For the fundamental wave, n has the value P, and the electrical angle between adjacent slots in the equivalent phase band is thus

$$2\theta = \frac{(360nX)}{N} \text{ degrees} \quad . \quad . \quad . \quad . \quad (6)$$

If the first conductor of the equivalent positive phase band is arbitrarily located at slot 1, the first conductor of the equivalent negative phase band must be located in such a position that the centre-lines of the two phase bands are mutually displaced by 180°. To satisfy this condition the number of the first slot of the negative band (see Section 12.2) must be $(M + 1)$, where

$$M = \frac{X}{2}(n_1 - n_2) \qquad (X \text{ even}) \quad . \quad . \quad . \quad (7a)$$

$$M = \frac{N}{2} + \frac{X}{2}(n_1 - n_2) \; (X \text{ odd}) \quad . \quad . \quad . \quad (7b)$$

A standard fractional-slot winding can now be defined as one in which

(*a*) $n_1 - n_2 = 0$ when $N/3$ is even.

$n_1 - n_2 = 1$ when $N/3$ is odd.

(*b*) In each phase band the angle between adjacent slots under the equivalent pole pair is 2θ degrees.

(*c*) The centre-lines of the two phase bands are separated by 180° under the equivalent pole pair.

The spread factor of any harmonic (see Section 12.3) including, of course, the fundamental wave ($n = P$) is given by

$$K_{de} = \frac{2\cos\left[(n_1 + n_2)\frac{\theta}{2}\right]\sin\left[(n_1 - n_2)\frac{\theta}{2}\right]}{(n_1 + n_2)\sin\theta} \; (nX \text{ even}) \quad (8a)$$

and $$K_{do} = \frac{2\sin\left[(n_1 + n_2)\frac{\theta}{2}\right]\cos\left[(n_1 - n_2)\frac{\theta}{2}\right]}{(n_1 + n_2)\sin\theta} \; (nX \text{ odd}) \quad (8b)$$

(4) EFFECT OF WINDING HARMONICS ON MACHINE OPERATION

Fractional-slot windings generate so-called sub-harmonics, i.e. those having pole pitches greater than that of the fundamental wave. In general, all harmonics or sub-harmonics are present, although in special cases certain orders of harmonics may be absent. The general harmonic series for one repeatable section of the winding (see Section 12.4) is thus

$$1, 2, 3, \ldots \quad n, n+1, n+2, \ldots$$

The term in which $n = P$ represents the fundamental wave, n less than P represents sub-harmonics and n greater than P represents harmonics.

These are magnetomotive-force harmonics which, saturation being ignored, produce corresponding flux-density waves in the air-gap, and since these waves, apart from the fundamental, do not rotate in synchronism with the rotor, they produce losses in the pole faces. With the equations given here the amplitudes of the harmonics can be calculated, and if necessary any particular harmonic likely to cause excessive pole-face loss can be reduced by the methods given in Section 5.

In some machines, however, particularly large hydro-electric generators and synchronous motors, an equally important consideration is the possibility of twice-line-frequency vibration on load of the stator core and frame due to sub-harmonics in the air-gap flux-density wave. Now, for a given amplitude of a force harmonic the maximum deflection of the core will be produced by the wave with the longest pole pitch, since the

deflection varies approximately with the cube of the pole pitch. It is therefore necessary to analyse the force wave in order to determine the amplitude of its lowest-order sub-harmonic.

There are two cases:

(*a*) *No even harmonics in flux-density wave* ($n_1 = n_2$).

The dominant component of the force wave with the longest pole pitch (see Section 12.5) is represented by

$$\left[\frac{A_pA_{p\pm2}}{P(P\pm2)}\right]\cos(2\theta+2\omega t)$$

(*b*) *Even harmonics in flux-density wave* ($n_1 \neq n_2$).

The corresponding wave is given here by

$$\left[\frac{A_pA_{p\pm1}}{P(P\pm1)}\right]\cos(\theta+2\omega t)$$

The force producing deflection is then proportional to the amplitudes of these waves, i.e.

$$\mathscr{P}_1 = \left[\frac{A_pA_{p\pm2}}{(P\pm2)}\right]P\ (n_1 = n_2) \quad . \quad . \quad . \quad (9a)$$

$$\mathscr{P}_2 = \left[\frac{A_pA_{p\pm1}}{(P\pm1)}\right]P\ (n_1 \neq n_2) \quad . \quad . \quad . \quad (9b)$$

It can be shown[10] that the radial deflection of the core by such waves is given by

$$u_1 = \frac{\mathscr{P}a}{E}\left[\frac{10c^3+18c^2+30c+6}{6(c-1)^3} - \frac{\sigma}{3}\right] \quad . \quad (10a)$$

(mode of vibration: 4-node)

i.e. a cos 2θ force-wave with $F = 1$ or a cos θ force-wave with $F = 2$

and by

$$u_2 = \frac{\mathscr{P}a}{E}\left[\frac{9c^7+9c^6+9c^5+25c^4+55c^3+7c^2+7c+7}{15(c-1)^3(c^4+4c^3+10c^2+4c+1)} - \frac{\sigma}{15}\right] \quad . \quad . \quad . \quad . \quad (10b)$$

(mode of vibration: 8-node)

i.e. a cos 2θ force-wave with $F = 2$ or a cos θ force-wave with $F = 4$. It is improbable on *a priori* grounds that higher modes will produce noise.

The case $F = 1$ with a cos θ force-wave corresponds to a winding in which P_T is a prime number and $n_1 \neq n_2$. This gives a rotating force-wave with the longest pole-pitch which is equivalent to a rotating unbalanced magnetic pull and which may lead not only to noise but also to mechanical vibration of the rotor. For these and other reasons this type of vibration is usually avoided and will not be considered further here.

The noise radiated by a machine is proportional to u^2DL. It is practically impossible to predict whether a particular machine will be noisy, and the best that can be done is to compare the value of u^2DL with that of a similar machine already built whose behaviour is known, noting that the noise in decibels is a function of log (u^2DL).[11]

Noise is not propagated unless the peripheral velocity of the force wave producing vibration is greater than the velocity of sound in air.[11] It thus follows that, corresponding to eqns. (10*a*) and (10*b*), there are critical core diameters above which load noise will be propagated and may thus be excessive. For 50 c/s machines these diameters are 7 and 14 ft respectively, so that, in general, noise and vibration due to these sub-harmonics are more likely to be troublesome in large machines than in small ones.

There is also the possibility of the vibration being amplified by resonance between the natural frequency of vibration of the core and the frequency of the sub-harmonic. This natural frequency is given approximately by

$$f_4 = \frac{2\cdot68}{2\pi}\sqrt{\frac{EIg}{\mu_1R^4}}\ \text{c/s} \quad . \quad . \quad . \quad . \quad (11a)$$

corresponding to eqn. (10*a*), and

$$f_8 = \frac{14\cdot55}{2\pi}\sqrt{\frac{EIg}{\mu_1R^4}}\ \text{c/s} \quad . \quad . \quad . \quad . \quad (11b)$$

corresponding to eqn. (10*b*).

(5) METHODS OF REDUCING WINDING HARMONICS

(5.1) Change in Width of Phase Bands

In a standard winding, $n_1 - n_2$ is zero or unity, corresponding to equal or nearly equal phase-band widths under the equivalent positive and negative poles. However, with a slight reduction in the value of K_d for the fundamental, the two phase bands may be made of unequal widths, since $n_1 - n_2$ is a disposable parameter. In eqn. (8*a*), $(n_1 - n_2)$ may be assigned, by inspection, such a value as will give for the required harmonic a minimum value of sin $[(n_1 - n_2)\theta/2]$ and thus a minimum value of the amplitude of the harmonic. Similar considerations apply to the cosine term in eqn. (8*b*).

(5.2) Interchange of Conductors at Ends of Equivalent Phase Bands

In some cases a change in the value of $n_1 - n_2$ may not give sufficient reduction in the amplitude of a given harmonic. A further reduction can then be obtained by interchanging conductors at the ends of the phase bands. This interchange may be applied either to a standard winding or to one in which $n_1 - n_2$ already has the optimum value for a particular harmonic as described in Section 5.1. In the latter case the harmonic will be subject to both reductions.

This interchange is shown in Fig. 1, in which conductor D, situated $(d - 1)/2$ slots from the right-hand end of equivalent

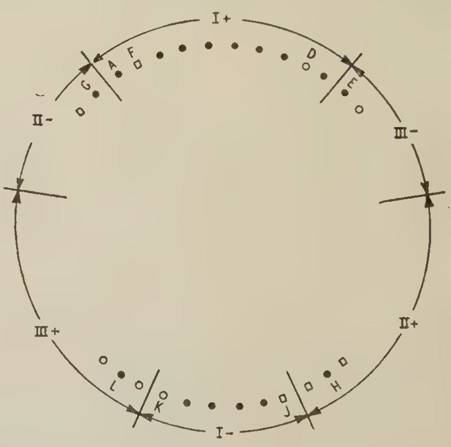

Fig. 1.—Displacement of conductors at ends of phase bands.

phase band I+, is interchanged with a conductor E, situated $(e - 1)/2$ slots from the left-hand end of phase band III−.

To preserve balance, conductors F, G, H, J and K, L must similarly be interchanged, the numbers *d* and *e* on phase band I− being reversed.

The value of the spread factor (see Section 12.6) for this new arrangement is given, for nX even, by

$$K_{de} = \frac{2\cos\left[(n_1+n_2)\frac{\theta}{2}\right]}{(n_1+n_2)}\left\{\frac{\sin\frac{m\theta}{2}}{\sin\theta} - 2\cos\left[(m-2d)\frac{\theta}{2}\right] + 2\cos\left[(m+2e)\frac{\theta}{2}\right]\right\} \quad (12a)$$

and, for nX odd, by

$$K_{do} = \frac{2\sin\left[(n_1+n_2)\frac{\theta}{2}\right]}{(n_1+n_2)}\left\{\frac{\cos\frac{m\theta}{2}}{\sin\theta} - 2\sin\left[(m-2d)\frac{\theta}{2}\right] + 2\sin\left[(m+2e)\frac{\theta}{2}\right]\right\} \quad (12b)$$

where $m = (n_1 - n_2)$. In both equations d and e can have only odd values.

If, as is frequently the case, $d = e$, both equations can be simplified to a form in which the spread factor for any particular harmonic in a standard winding need only be multiplied by a factor K_f to obtain the spread factor for the rearranged winding, thus

$$K_f = (1 - 4\sin\theta\sin d\theta) \quad . \quad . \quad . \quad . \quad (13)$$

It is obvious that this rearrangement need not be confined to the interchange of a pair of conductors at the edge of each phase, but may be extended to any suitable number of pairs of conductors; for example, for two pairs of conductors interchanged, eqn. (12a) becomes

$$K_{de} = \frac{2\cos\left[(n_1+n_2)\frac{\theta}{2}\right]}{(n_1+n_2)}\left\{\frac{\sin\frac{m\theta}{2}}{\sin\theta} - 2\cos\left[(m-2d)\frac{\theta}{2}\right] - 2\cos\left[(m-2d_1)\frac{\theta}{2}\right] + 2\cos\left[(m+2e)\frac{\theta}{2}\right] + 2\cos\left[(m+2e_1)\frac{\theta}{2}\right]\right\} \quad (14a)$$

and eqn. (12b) becomes

$$K_{do} = \frac{2\sin\left[(n_1+n_2)\frac{\theta}{2}\right]}{(n_1+n_2)}\left\{\frac{\cos\frac{m\theta}{2}}{\sin\theta} - 2\sin\left[(m-2d)\frac{\theta}{2}\right] - 2\sin\left[(m-2d_1)\frac{\theta}{2}\right] + 2\sin\left[(m+2e)\frac{\theta}{2}\right] + 2\sin\left[(m+2e_1)\frac{\theta}{2}\right]\right\} \quad (14b)$$

These two equations may be similarly extended for three or more interchanges on each equivalent phase band.

(5.3) Displacement of Repeatable Sections

This method of reducing harmonics is simply a method of obtaining what is in effect double chording.[7] Normal chording or pitching of a winding produces a circumferential displacement of the top layer of conductors with respect to the bottom layer, the corresponding pitch factor being

$$K_p = \sin(nS_1\pi/N) \quad . \quad . \quad . \quad . \quad . \quad (15)$$

where S_1 is the pitch of a coil expressed in number of slots.

In Fig. 1 the conductors in the positive equivalent phase band of phase I are designated by I+; if the winding can be divided into two repeatable sections the conductors in the positive equivalent phase band of the other section, designated by I_1+, are in phase with the corresponding conductors of the other phase band, as shown in Fig. 2(a).

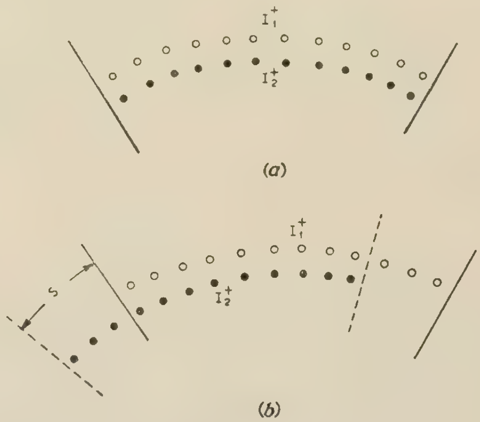

Fig. 2.—Displacement of repeatable sections.

(a) Sections in phase. (b) Sections out of phase.

It can be seen from Fig. 2(b) that it is a simple matter to arrange the conductors of I_1+ out of phase with those of I_2+ by any desired number of slot pitches under the equivalent pole-pair, i.e. by any desired electrical angle. Corresponding changes must be obviously made to I_2- to preserve symmetry and balance. The resulting pitch factor for the existing harmonics (see Section 12.7) will be given by

$$K_{p1} = \cos(nXS180/N) \quad . \quad . \quad . \quad . \quad (16)$$

where S represents the number of slots under the equivalent pole pair by which the two sections are displaced with respect to one another.

The same analysis can be applied to three or more sections displaced with respect to one another.

This displacement of, e.g., two repeatable sections produces in effect a winding of only one section, i.e. the pattern of conductors in a phase repeats only once instead of twice in one circuit of the machine.

It thus follows that the general harmonic series given in Section 4 now has the additional order of harmonics represented by 1/2, 3/2, 5/2 . . . introduced by the displacement of two repeatable sections, and a check must be made on the amplitudes of the new harmonics in the force wave to ensure that none is greater than those previously existing.

The pitch factor for the harmonics introduced by the displacement is

$$K_{p2} = \sin(nXS180/N) \quad . \quad . \quad . \quad . \quad (17)$$

(6) METHODS OF CONNECTING A WINDING

The selection of the method of connection of a given winding for large machines is, in general, decided by economic and mechanical considerations, although reduction of eddy-current

losses and the design of coil insulation may play decisive roles. The discussion here will be restricted to the characteristics of lap and wave windings considered purely as methods of connection. With multi-turn coils lap windings are always used, but with single-turn (bar) coils lap windings are usually employed only with small numbers of poles ($P_T < 4$); with larger values of P_T wave windings generally give a lower weight of copper, for although the weight of the end windings is increased by the inherent 100% winding pitch of wave windings, this is more than counterbalanced by the reduction in the weight of connectors.

It is frequently necessary to connect a winding so as to obtain two or more circuits in parallel per phase, and the possible number of such parallel circuits is, of course, given by the common factors of $2P_T$ and $N_T/3$. For example, in connecting two circuits in parallel in a winding with two repeatable sections ($F = 2$) it is only necessary to ensure that, if any conductor number v is in one circuit, conductor number $(v + N)$ is in the other circuit. If, however, the winding has only one section, it is necessary to make the usual check to ensure that the two circuits are in phase. Similar considerations apply to three or more parallel circuits.

(6.1) Connection of Wave Windings

In medium and large a.c. machines, two-layer wave windings are almost invariably made from single bars or coil sides, the appropriate connections being made by soldering or brazing at the back and front of the winding. Such a winding permits great flexibility both in the choice of coil pitches and in the arrangement of end connections, but analytical procedure has largely to be abandoned and replaced by a few simple rules. By the application of these rules and, in some cases, the expenditure of time and ingenuity by the designer, a satisfactory wave winding can usually be obtained.

In designing a wave winding it is important to remember that the winding pitch is established in the electrical design of the winding; in designing the end-winding and connections the coil pitch may have any value required to give the best overall mechanical design.

The simple rules mentioned above are, for clarity, explained by means of practical examples.

(6.2) Connection of Lap Windings

Most lap windings are designed with multi-turn coils and the coil pitch is then identical with the winding pitch. The methods used for connecting these windings are simple and well known.

(6.3) Selection of Electrical and Coil Pitch of a Winding

The fractional-slot winding differs from the integral-slot winding in that different pitches are available in the former according to whether it is an overpitch or an underpitch, whilst in the latter an underpitch of 1, 2, etc., slot pitches is exactly equivalent electrically to an overpitch of 1, 2, etc., slot pitches. In the multi-turn lap winding this property of the fractional-slot winding is not of much practical importance, since overpitching leads to a substantial increase in the length of the end windings and thus in the weight of copper and in the losses. In the wave winding, however, overpitching has no appreciable effect on the weight of copper, since the average coil pitch must always be about 100%, irrespective of the electrical pitch.

(7) EXAMPLE OF APPLICATION OF DESIGN PROCEDURE ($x = 1$)

(7.1) Design of the Standard Winding

The following example is that of a large vertical hydro-electric generator in which the number of stator slots is 306 and the number of poles 28. The significant parameters are $N_T = 306$, $P_T = 14$, and the highest common factor of these two parameters is $F = 2$. The equivalent machine thus has 153 stator slots of 7 pairs of poles, i.e. $N = 153$, $P = 7$. From eqn. (4) for $x = 1$

$$\frac{xN}{P} = \frac{153}{7} = 21\tfrac{6}{7} = 22 - \tfrac{1}{7}$$

so that the pitch of the slots under the equivalent pair is 22 ($= X$). Now $N/3 = 51$, so that $n_1 + n_2 = 51$ and, for a standard winding, $n_1 - n_2 = 1$, giving $n_1 = 26$ and $n_2 = 25$. From eqn. (6), $2\theta = 362\tfrac{6}{17}°$, or $2\tfrac{6}{17}°$ in the first quadrant. Starting from slot 1 and taking a pitch of $X = 22$ slots with the value of $2\theta = 2\tfrac{6}{17}°$, Table 1 can be compiled for the arrangement of bottom conductors in one phase. This will give $n_1 = 26$ slots uniformly distributed over a phase band under the equivalent positive pole with a width of $(58\tfrac{14}{17} + 2\tfrac{6}{17}°)° = (60° + \theta)$.

The n_2 (=25) slots under the equivalent negative pole are required to satisfy similar conditions but to be displaced by 180 electrical degrees from the phase band under the positive pole. To satisfy this latter condition the first slot of this band is found from eqn. (7) to be slot 12, i.e. $M = 11$. The corresponding arrangement is given in Table 2.

For ready application of these Tables to the layout of the winding, the slots in both phase bands are rearranged in sequence, as shown in Table 3.

In order to obtain the arrangement of the bottom conductors in the other two phases, Table 4 is prepared in the following manner. The phase-I column in this Table is obtained by writing 4 to represent slots 1, 2, 3, 4 in Table 3 under a positive pole, the next represents slots 12, 13, 14, 15 under a negative pole and so on. The ring round the top '4' indicates that the first conductor of this group is arbitrarily selected as the start of phase I. The start of phase II will be $2P$ (=14) phase groups

Table 1

Slot Numbers and Angles on Phase I+ under Equivalent Pole Pair. Standard Winding

$P = 7$, $N = 153$. $n_1 = 26$, $n_2 = 25$

Slot No. .. Angle (deg) .. 2θ (deg) ..	1 0 0	23 2·4 362·4	45 4·7 724·7	67 7·1 etc.	89 9·4	111 11·8	133 14·1	2 16·5	24 18·8	46 21·2	68 23·5
Slot No. .. Angle (deg) ..	90 25·9	112 28·2	134 30·6	3 32·9	25 35·3	47 37·6	69 40	91 42·4	113 44·7	135 47·1	4 49·4
Slot No. .. Angle (deg) ..	26 51·8	48 54·1	70 56·5	92 58·8							

Table 2

SLOT NUMBERS AND ANGLES ON PHASE I– UNDER EQUIVALENT POLE PAIR. STANDARD WINDING

$P = 7, N = 153. \quad n_1 = 26, n_2 = 25$

Slot No. .. Angle (deg) ..	12 181·2	34 183·5	56 185·9	78 188·2	100 190·6	122 192·9	144 195·3	13 197·6	35 200	57 202·4	79 204·7
Slot No. .. Angle (deg) ..	101 207·1	123 209·4	145 211·8	14 214·1	36 216·5	58 218·8	80 221·2	102 223·5	124 225·9	146 228·2	15 230·6
Slot No. .. Angle (deg) ..	37 232·9	59 235·3	81 237·6								

Table 3

SLOT NUMBERS, IN SEQUENCE, ON PHASES I+ AND I– UNDER EQUIVALENT POLE PAIR. STANDARD WINDING

$P = 7, N = 153. \quad n_1 = 26, n_2 = 25$

Positive poles	1, 2, 3, 4 89, 90, 91, 92	23, 24, 25, 26 111, 112, 113	45, 46, 47, 48 133, 134, 135	67, 68, 69, 70
Negative poles	12, 13, 14, 15 100, 101, 102	34, 35, 36, 37 122, 123, 124	56, 57, 58, 59 144, 145, 146	78, 79, 80, 81

further on, and that of phase III will be $4P$ (=28) phase groups from the start of phase I. The starts of the other two phase groups having thus been determined, the pattern of groups of conductors must in each case follow that of phase I as shown. Table 4 thus gives the position of all the bottom conductors in all three phases.

Table 4

ARRANGEMENT OF BOTTOM CONDUCTORS IN ALL THREE PHASES STANDARD WINDING

$P = 7, N = 153. \quad n_1 = 26, n_2 = 25$

+ or – pole	Ph. I	Ph. III	Ph. II
+	4	4	3
–	4	4	3
+	4	4	3
–	4	4	3
+	4	3	4
–	4	3	4
+	4	3	4
–	4	3	4
+	4	3	4
–	3	4	4
+	3	4	4
–	3	4	4
+	3	4	4
–	3	4	3

From eqn. (8*a*) the value of K_{de} for the fundamental is found to be 0·956.

The electrical winding pitch is $S_1 = 9$, so that from eqn. (15) the pitch factor K_p of the fundamental is 0·963. Since $n_1 \neq n_2$ the harmonic nearest in order to the fundamental is the 8th and the force harmonic with the longest pole pitch [from eqn. 9*b*)] is

$$\mathscr{P}_2 = \left(\frac{A_7A_8}{8}\right)7$$

Again, from eqn. (8*a*) the value of K_{de} for the 8th harmonic is 0·041 7 and $K_p = 0·995$, so that $\mathscr{P}_2 = 3·35 \times 10^{-2}$.

Since the force wave is of the form cos θ and since $F = 2$ and $L = 25$, the value of u_1 is found from eqn. (10*a*) to be $0·908 \times 10^{-3}$. The noise energy is proportional to

$$(u_1)^2DL, \text{ which is } 48·2 \times 10^{-4}$$

From eqn. (11*a*) the natural frequency of vibration of the core, f_2, is 12·4 c/s, so that there is no danger of resonance with the force wave, which is at twice line frequency.

(7.2) Reduction of Harmonic by Changing Relative Width of Phase Bands

The machine in question emitted a pronounced 100 c/s noise on load; in order to reduce the ($n = 8$) harmonic, a change in the relative widths of the phase bands, as explained in Section 5.1, was investigated. In eqn. (8*a*), for $n = 8$ we have $\sin\left[(n_1 - n_2)\frac{\theta}{2}\right] = \sin\left[(n_1 - n_2)76·47\right]$. Since $N/3\,(= n_1 + n_2)$ is odd, $n_1 - n_2$ must also be odd, and, by inspection, $n_1 - n_2 = 5$ gives the lowest value of this sine term. The spread factor of this subharmonic then becomes 0·016 5, a reduction of more than 60%. The spread factor of the fundamental wave is practically unchanged. The resulting value of $(u_1)^2DL$ is now $7·56 \times 10^{-4}$.

Since $n_1 + n_2 = 51$ and $n_1 - n_2 = 5$, we have $n_1 = 28$ and $n_2 = 23$; the positive equivalent phase band thus contains 28 conductors spread over $65\frac{15}{17}°$ and the negative band contains 23 spread over $54\frac{2}{17}°$. This requires Table 1 to be extended by a further 2 conductors, i.e. 114 and 136, and Table 2 to be reduced by 2 conductors, i.e. 12 and 34. The new winding arrangement with the conductors arranged in number sequence as in Table 3 is given in Table 5. As before, these sequences can be used to construct Table 6, showing the location of bottom conductors in all three phases.

(7.3) Reduction of Harmonic by Interchange of Conductors

To obtain a possibly greater reduction in the 8th harmonic a tentative interchange of the conductors at the ends of the equivalent phase bands was also investigated. Thus, in eqn. (13) let $d = e = 1$ so that the reduction factor K_f for the spread

Table 5

SLOT NUMBERS, IN SEQUENCE, OF PHASES I+ AND I− UNDER EQUIVALENT POLE PAIR. MODIFIED WINDING

$P = 7, N = 153.\quad n_1 = 28, n_2 = 23.$

Positive poles	1, 2, 3, 4 89, 90, 91, 92	23, 24, 25, 26 111, 112, 113, 114	45, 46, 47, 48 133, 134, 135, 136	67, 68, 69, 70
Negative poles	13, 14, 15 100, 101, 102	35, 36, 37 122, 123, 124	56, 57, 58, 59 144, 145, 146	78, 79, 80, 81

factor of the 8th harmonic, as compared to its value in the standard winding, is 0·173. Similarly K_f of the fundamental wave is 0·998. The value of $(u_1)^2DL$ is then $1{\cdot}44 \times 10^{-4}$.

In Table 1 conductors 1 and 92 are removed and replaced by conductors 132 and 114, and in Table 2 conductors 12 and 81 are replaced by conductors 143 and 103. The tabular representation of the conductors in sequence, given in Table 7, can be obtained directly from Table 3 by adding and subtracting conductors as stated above.

The interchange of conductors could, of course, have been made to the arrangement of wide and narrow phase bands.

(7.4) Reduction of Harmonic by Displacement of Repeatable Sections

The pitch factor for displacement of the two sections in this example is $K_{p8} = \cos(207\tfrac{1}{17}S)$ for the first sub-harmonic. It can be seen that a minimum value of this factor is obtained with $S = 3$ so that $K_{p8} = 0{\cdot}153$. Then for the fundamental, $K_p = 0{\cdot}999$ and the resulting value of $(u_1)^2DL$ is $1{\cdot}13 \times 10^{-4}$. However, there is a complication here, since, as stated in Section 5.3, the displacement of the sections introduces a new series of harmonics of which the one of the longest pole pitch is of the form $\cos\theta$ with $F = 1$.

For reasons already stated this is undesirable and would not, in this case, normally be considered as a satisfactory method of reducing noise. For the purpose of exposition, however, the design of this variant of the standard winding with $S = 3$ will be worked out in detail.

The layout of the second repeatable section in the standard winding is exactly the same as that in Tables 1–4 except that 153 is added to all the conductor numbers. To shift this second section with respect to the first section, by three slots, the first three conductors in Table 1 (1, 23 and 45) are omitted and the three conductors 114, 136 and 5 added. Similarly, in Table 2 conductors 12, 34, and 56 are omitted and conductors 103, 125 and 147 added. The number Table for the whole winding will then be as shown in Table 8; the top half is a duplicate of Table 4 and the bottom half includes the modification mentioned above.

An inspection of this Table shows that, as would be expected, the second half of the winding in, e.g., phase I has the same cyclic grouping of conductors under successive poles as in the first half except that the relative position of corresponding groups in the two sections has been moved 3 (=S) pole pairs. In general, the relative shift is Sx pole pairs (see Section 12.8). This fact permits Table 8 to be compiled for a given value of S directly from Table 4, assuming that a standard winding is used for each section. The procedure will be exactly the same for non-standard windings.

(7.5) Comparison of Results

The audible noise in decibels is a function of $10 \log DLu^2/10$ and the values for the four cases are as follows:

Standard winding	= − 33·2 dB
$n_1 - n_2 = 5$	= − 41·3 dB
$d = e = 1$	= − 48·4 dB
$S = 3$	= − 49·5 dB

These values show that, taking the noise of the standard winding as the reference level, the first modification will reduce the noise by 8·1 dB, the second by 15·2 dB, the third by 16·3 dB. For reasons already given, the last alternative is not desirable but the third ($d = e = 1$) would give a substantial reduction in

Table 6

ARRANGEMENT OF BOTTOM CONDUCTORS IN ALL THREE PHASES MODIFIED WINDING

$P = 7, N = 153.\quad n_1 = 28, n_2 = 23$

+ or − pole	Ph. I	Ph. III	Ph. II
+	4	4	4
−	3	4	3
+	4	4	4
−	3	4	3
+	4	3	4
−	4	4	3
+	4	3	4
−	4	4	3
+	4	3	4
−	3	4	4
+	4	3	4
−	3	4	4
+	4	3	4
−	3	4	3

Table 7

ARRANGEMENT OF BOTTOM CONDUCTORS IN ALL THREE PHASES MODIFIED WINDING

$P = 7, N = 153.\quad n_1 = 26, n_2 = 25, d = e = 1$

+ or − pole	Ph. I	Ph. III	Ph. II
+	3	4	4
−	3	4	3
+	4	3	4
−	4	3	4
+	4	4	3
−	4	4	3
+	4	3	4
−	3	4	4
+	3	4	4
−	4	3	4
+	4	3	4
−	3	4	3
+	4	4	3
−	4	4	4

Table 8

ARRANGEMENT OF BOTTOM CONDUCTORS IN ALL THREE PHASES

$P = 7, N = 153. \quad n_1 = 26, n_2 = 25, S = 3$

+ or − pole	Ph. I	Ph. III	Ph. II
+	4	4	3
−	4	4	3
+	4	4	3
−	4	4	3
+	4	3	4
−	4	3	4
+	4	3	4
−	4	3	4
+	4	3	4
−	3	4	4
+	3	4	4
−	3	4	4
+	3	4	4
−	3	4	3
+	4	3	4
−	3	4	4
+	3	4	4
−	3	4	4
+	3	4	4
−	3	4	3
+	4	4	3
−	4	4	3
+	4	4	3
−	4	4	3
+	4	3	4
−	4	3	4
+	4	3	4
−	4	3	4

the noise level; owing, however, to the delay involved in reconnecting a heavy-current high-voltage stator winding this modification was not made. On site the machine was erected in a pit below floor level and the 100 c/s vibration and noise were therefore scarcely perceptible in the station.

(8) CONNECTION OF WAVE WINDINGS

In designing the layout of the end windings and connections for wave windings certain conventions are assumed which considerably simplify the work and which are most conveniently explained by reference to an actual example.

(8.1) Hydro-Electric Generator with Standard Winding

This example corresponds to the standard winding of which the electrical design is given in Section 7.1. Broadly speaking, there are two methods of designing the end windings. They are discussed in Sections 8.1.1 and 8.1.2.

(8.1.1) Short and Long Coil Pitches.

This method involves the use of a back coil pitch which corresponds to the required electrical pitch of the winding and a front coil pitch which is selected so that the sum of the back and front pitches is equal to the integer nearest to N/P. Since the back coil pitch is equal to the winding pitch, the pattern of the top conductors will follow that of the bottom conductors but displaced by S_1 slots. With this arrangement there is no need to make special connections at the back of the winding, and it is therefore only necessary to consider bottom conductors, i.e. those which, viewed from inside the stator core, have the shape shown in Fig. 3. Successive bottom conductors will therefore

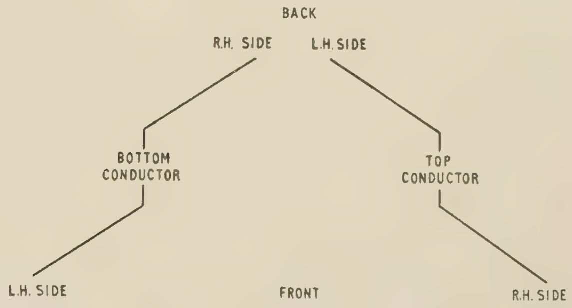

Fig. 3.—Designation of coils of wave winding.

be in a sequence given by the double coil pitch, i.e. the sum of the back and front coil pitches.

Referring now to Fig. 4, the layout of the winding proceeds as follows. The back coil pitch is from slots 1 to 10, corre-

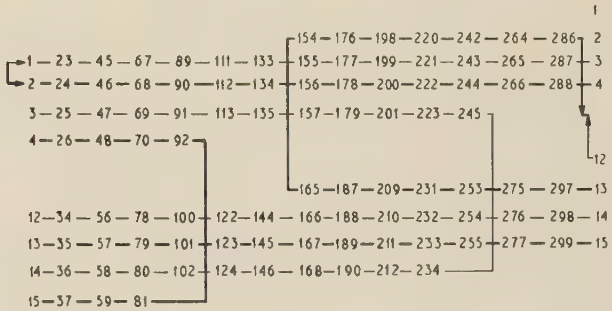

Fig. 4.—Standard wave winding. Number diagram: $P = 7, N = 153, n_1 = 26, n_2 = 25$. Winding pitch, slots 1–10, coil pitch, 1–10 back, 1–14 front.

sponding to an electrical pitch of 82·3%. Since the integer nearest to N/P is 22, the double coil pitch is preferably 22 and the front coil pitch is $(22 - 9) = 13$, i.e. from slots 1 to 14.

The groups of conductors given in Table 4 for phase I are set out in vertical columns as shown in Fig. 4, each vertical column representing positive (alternate) poles. This winding has two repeatable sections, so that the second half of the winding is formed by adding $N_T/2$ (=153) to all the slots in the first half of the winding. As can be seen, the horizontal rows are formed by adding 22 to the numbers in the first vertical column, then to the next vertical column, and so on. The negative circuit is formed from Table 4 in a similar manner, the vertical columns representing the negative (alternate) poles. This number Table having been produced, the connection for two parallel circuits per phase proceeds as follows. A preliminary step, which may have to be modified later, is to connect as many conductors as possible with short horizontal lines, since these represent a regular progression through the winding with no special connectors. In doing this, care must be taken that, for any conductor a included in one circuit, the conductor in phase with it (i.e. conductor $a + N_T/2 = a + 153$) is in the other circuit, in order to ensure balance and phase equality between the two circuits. The next

step is to insert the jumpers (connectors) to enable the conductors under both the positive and negative poles to be connected into their appropriate circuit, noting that, by convention, progress through the positive poles in Fig. 4 is from left to right and through the negative poles from right to left. The position of these jumpers having been settled, each circuit should close on itself and include $\frac{1}{2}(N_T/3) = 51$ conductors. The two circuits can then be opened at two convenient points and the start and end of the phase can be established.

In deciding on the positions of the jumpers and the start and end of the winding, the general rule is to select an arrangement which gives the minimum length of jumpers and paralleling connections, as shown in Fig. 4.

(8.1.2) Equal Pitches Back and Front.

The winding just described, although giving a simple arrangement of jumpers and paralleling connectors, has a substantially longer overhang at the front of the winding than at the back. As already shown, if this winding were overpitched the longer overhang would be at the back of the machine. In some larger machines of relatively high speed it is an advantage to make the front and back pitches equal from the point of view of mechanical layout of the whole machine and also of equalizing the stresses in the end windings under short-circuit conditions. This result is obtained by the method shown in Fig. 5. The top and bottom conductors of a portion of the repeatable section of the standard winding discussed in Section 7.1 are shown in Fig. 5(a). Now, it can be seen that conductors can be moved from the top of a

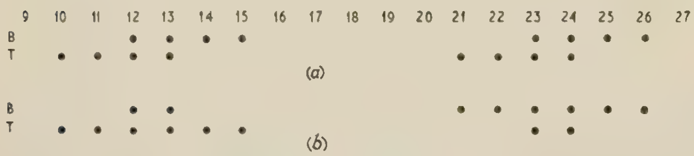

Fig. 5.—Conductor diagram. Transposition of top and bottom conductors in a slot.

(a) Normal arrangement. (b) Arrangement for equalizing front and back pitches.

slot to the bottom of the same slot without any change in the electrical characteristics of the winding as a whole. It is therefore possible to move all the available conductors under successive poles to the bottoms and tops of the slots, as shown in Fig. 5(b). Thus, under the first pole, conductor 153 is moved from the top of the slot to the bottom of the same slot under the second pole, conductors 14 and 15 are moved from the bottoms of the slots to the tops, and so on for the remaining poles. Fig. 5(b) can now be translated into a connection diagram as shown in Fig. 6.

It will be noted that in this diagram both top and bottom conductor numbers are shown, whilst in Fig. 4 only bottom conductors are shown. As already stated, this is necessary if jumpers are to be fitted at the back of the winding. In general, but not invariably, when conductors have been moved from the bottom to the top of the slot and vice versa and parallel circuits are required, a better connection arrangement is obtained by fitting jumpers at the back, and it is then necessary to show both top and bottom conductors in the winding diagram (Fig. 6), since the pattern of the top conductors does not follow that of the bottom conductors. It must, however, be emphasized here that moving the position of the conductors in the slots does not affect the electrical pitch of the winding, which is determined only by the electrical angle between the various conductors around the stator core. Moving the conductors in the slots merely permits a change in the coil pitch.

Since in Fig. 4 the double coil pitch is 22 slots, in Fig. 6 the back and front coil pitches may each be equal to 11 slot pitches, i.e. from slots 1 to 12. In connecting up, the same procedure is followed as in Fig. 4, except that additional rules must be observed. Referring to Fig. 3 it can be seen that the number representing a bottom coil side can be connected on its right-hand side to the left-hand side of a number representing a top coil side. Similarly a number representing the left-hand side of a bottom coil side can be connected to the right-hand side of a number representing a top coil side. It is not possible to connect the right-hand side of a bottom conductor with the right-hand side of a top conductor or conversely. The connection diagram shown in Fig. 6 is straightforward and requires no further comment.

(9) CONCLUSIONS

A simple method of calculating the electrical design of a fractional-slot winding and of determining the amplitude of the subharmonics and harmonics generated in the air-gap flux-density wave has been developed. It has been shown that interaction between the fundamental of this wave and the harmonic nearest in order to the fundamental produces a force wave which may lead to vibration and noise on load. This leads to the conclusion that machines having a number of pole pairs which is prime should, if possible, not have windings in which the number of slots per phase is odd. Three different methods of modifying a standard winding in order to reduce the amplitude of any particular harmonic have been evolved; the method of displacement of repeatable sections is unlikely to be of great practical value in the lessening of noise, since by reducing one particular harmonic a further series of undesirable harmonics is introduced. In large low-speed synchronous generators the choice between lap winding and wave winding is determined largely

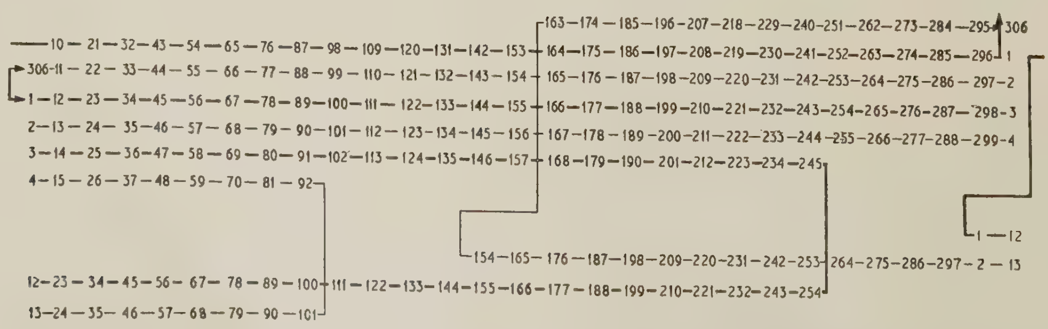

Fig. 6.—Standard wave winding. Number diagram: $P = 7$, $N = 153$, $n_1 = 26$, $n_2 = 25$. Winding pitch, slots 1–10, coil pitch, 1–12 back and front.

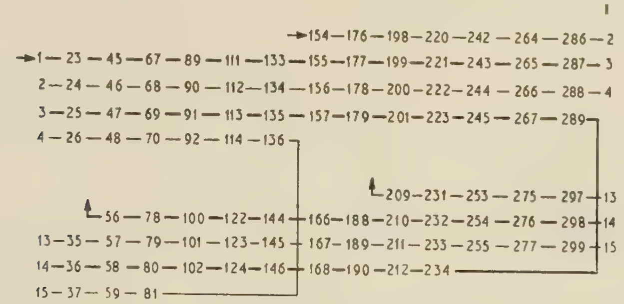

Fig. 7.—Modified wave winding. Number diagram: $P = 7$, $N = 153$, $n_1 = 28$, $n_2 = 23$. Winding pitch, slots 1–10, coil pitch, 1–10 back, 1–14 front. (See Table 6.)

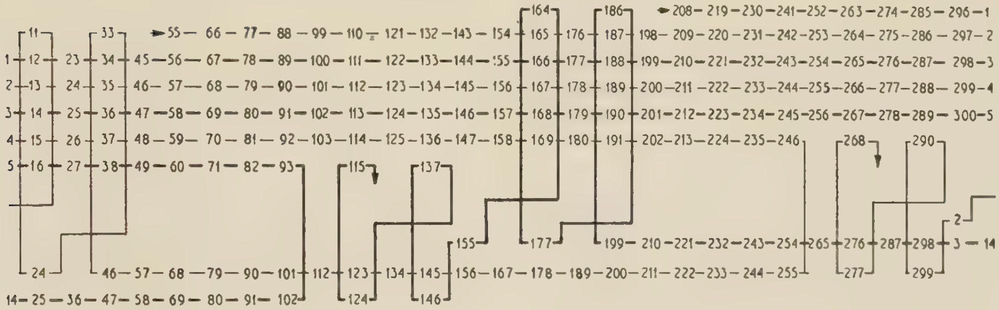

Fig. 8.—Modified wave winding. Number diagram: $P = 7$, $N = 153$, $n_1 = 28$, $n_2 = 23$. Winding pitch, slots 1–10, coil pitch, 1–12, back and front. (See Table 6.)

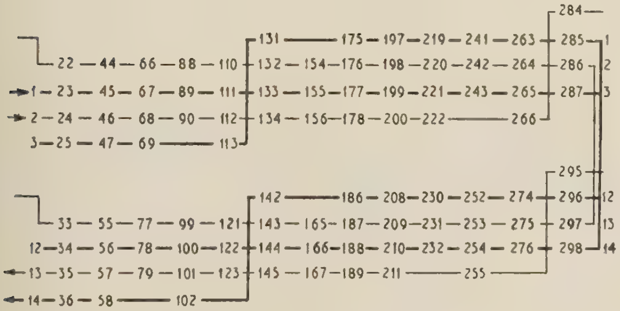

Fig. 9.—Modified wave winding. Number diagram: $P = 7$, $N = 153$, $n_1 = 26$, $n_2 = 25$, $d = e = 1$. Winding pitch, slots 1–10, coil pitch, 1–10 back, 1–14 front. (See Table 7.)

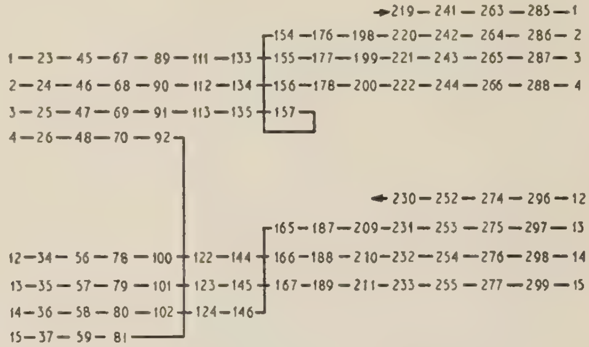

Fig. 11.—Modified wave winding. Number diagram: $P = 7$, $N = 153$, $n_1 = 26$, $n_2 = 25$, $S = 3$. Winding pitch, slots 1–10, coil pitch, 1–10 back, 1–14 front. (See Table 8.)

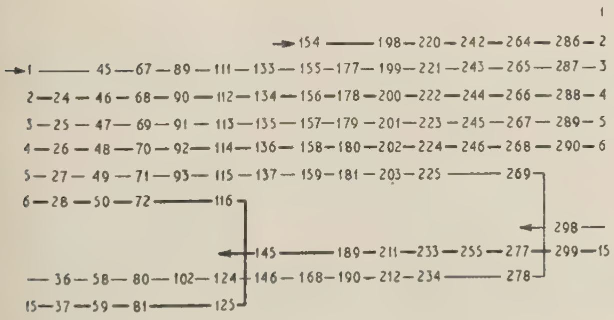

Fig. 10.—Modified wave winding. Number diagram: $P = 7$, $N = 153$, $n_1 = 26$, $n_2 = 25$, $d = e = 1$. Winding pitch, slots 1–10, coil pitch, 1–12, back and front. (See Table 7.)

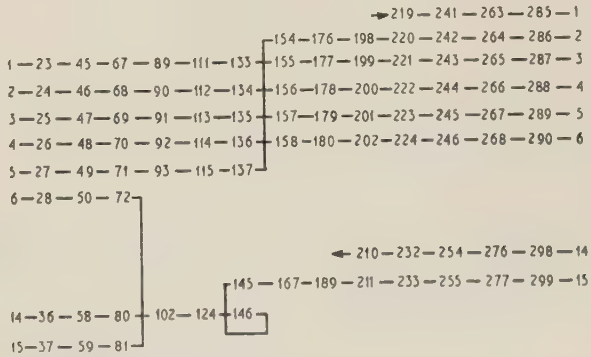

Fig. 12.—Modified wave winding. Number diagram: $P = 7$, $N = 153$, $n_1 = 26$, $n_2 = 25$, $S = 3$. Winding pitch, slots 1–10, coil pitch, 1–12, back and front. (See Table 8.)

by mechanical considerations, and in these machines the wave winding offers decided advantages from the point of view of simplicity of end connections.

(10) ACKNOWLEDGMENTS

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(12) APPENDICES

(12.1) Determination of Optimum Widths of Equivalent Phase Bands

The value of K_d for the fundamental is obtained by putting $n = P$ in eqn. (18), developed in Section 12.3. Since, from eqn. (4), $xN = PX \pm 1$ eqn. (18) may be written

$$K_d = \frac{\sin(n_1\theta) + (-1)^{xN}\sin(n_2\theta)}{(n_1 + n_2)\sin\theta}$$

It is required to find values of n_1 and n_2 such that K_d is a maximum, provided that $n_1 + n_2 = N/3$ and is a constant.

There are two main cases to be considered, depending on whether N is even or odd.

N even.

This implies that $n_1 + n_2$ and $n_1 - n_2$ are both even.

$$K_d = \frac{\sin(n_1\theta) + \sin(n_2\theta)}{(n_1 + n_2)\sin\theta}$$

$$= \frac{2\sin\left[\left(\frac{n_1+n_2}{2}\right)\theta\right]\cos\left[\left(\frac{n_1-n_2}{2}\right)\theta\right]}{(n_1 + n_2)\sin\theta}$$

Hence, since $n_1 + n_2$ is constant, K_d is a maximum when $\cos\left[\frac{(n_1 - n_2)\theta}{2}\right]$ is a maximum, i.e. when $n_1 = n_2$ and $\cos\left[\frac{(n_1 - n_2)\theta}{2}\right] = 1$ or K_d is a maximum when $n_1 - n_2 = 0$.

N odd.

This implies that $n_1 + n_2$ and $n_1 - n_2$ are both odd.

$$K_d = \frac{\sin(n_1\theta) + (-1)^{xN}\sin(n_2\theta)}{(n_1 + n_2)\sin\theta}$$

There are now two variants, depending on whether x is odd or even. It can be shown in the same manner as for 'N even' that in both these cases K_d is a maximum when $n_1 - n_2 = 1$.

(12.2) Slot Number of First Conductor in the Negative Phase Band

Let the first conductor in the positive phase band be A. Then, referring to Fig. 1, conductor J is $(N/2) + [(n_1 - n_2)/2]$ positions away.

The interval between two consecutive positions in Fig. 1 corresponds to a jump of X slots on the actual machine, and we have

$$\text{Number of slots between A and J} = \frac{(N + n_1 - n_2)X}{2}$$

This can be simplified, since a jump of N slots comes back to the same slot, and the expression reduces to $\frac{1}{2}X(n_1 - n_2)$ when X is even, and $[\frac{1}{2}X(n_1 - n_2) + \frac{1}{2}N]$ when X is odd. This means that, if, for example, the positive phase band starts in slot No. 1, the negative phase band (if X is even) starts in slot No. $\left[1 + \frac{X(n_1 - n_2)}{2}\right]$.

(12.3) Determination of Spread Factor (K_d) for Harmonic n

Let ψ be the angle between the axes of symmetry of the positive and negative phase bands of one phase, referred to the harmonic n; then $\psi = \theta N = (nX\,180°)$.

Let ϕ_1 be the angle between a typical slot in the positive band and the axis of symmetry of that band.

Then ϕ_1 has the values

$$-(n_1 - 1)\theta,\ -(n_1 - 3)\theta \ldots (n_1 - 3)\theta,\ (n_1 - 1)\theta$$

$$\text{Hence } \Sigma(\cos\phi_1) = \cos[-(n_1 - 1)\theta] + \cos[-(n_1 - 3)\theta] + \ldots + \cos[(n_1 - 3)\theta] + \cos[(n_1 - 1)\theta] = \frac{\sin(n_1\theta)}{\sin\theta}$$

The values of the angle ϕ_2, between a typical slot in the negative phase band and the axis of symmetry of the positive phase band, are $[\psi - (n_2 - 1)\theta]$, $[\psi - (n_2 - 3)\theta]$, . . . $[\psi + (n_2 - 3)\theta]$, $[\psi + (n_2 - 1)\theta]$. Hence

$$\Sigma(\cos\phi_2) = (-1)^{nX}\left[\frac{\sin(n_2\theta)}{\sin\theta}\right]$$

Since the currents in the negative groups are reversed the sum of cosines for the $(n_1 + n_2)$ slots is

$$\Sigma\cos\phi = \frac{1}{\sin\theta}[\sin(n_1\theta) - (-1)^{nX}\sin(n_2\theta)]$$

Hence
$$K_d = \frac{\sin(n_1\theta) - (-1)^{nX}\sin(n_2\theta)}{(n_1 + n_2)\sin\theta} \quad . \quad . \quad . \quad (18)$$

(12.4) Presence of Harmonics

(12.4.1) Harmonics due to Current in One Phase.

From eqn. (18) it is apparent that in general K_d is not zero, i.e. all harmonics and sub-harmonics are present.

From eqn. (18) it can be seen that the even harmonics do not necessarily vanish when N is even. The condition $n_1 = n_2$ must also be satisfied.

(12.5) Harmonic Analysis of Current Distribution, Flux Density, and Magnetic Force

(12.5.1) Current Distribution due to One Phase.

Consider the angles of the slots occupied by the top-layer conductors of one phase of a 3-phase winding. In a balanced winding, there is an axis about which these angles are symmetrical. Because of this symmetry, the harmonic analysis of the current-density distribution when there is unit current through the winding is of the form

$$b_0 + b_1 \cos \theta + b_2 \cos 2\theta + \ldots + b_n \cos n\theta + \ldots = \Sigma b_n \cos n\theta$$

The current-density distribution due to the current in the bottom-layer conductors, which are displaced by an angle α from the top conductors, is

$$-[b_0 + b_1 \cos(\theta - \alpha) + \ldots b_n \cos n(\theta - \alpha)] = -\Sigma b_n \cos n(\theta - \alpha)$$

Combining these two expressions to get the resultant current-density distribution due to unit current in one phase, we obtain

$$-2\Sigma b_n \sin n(\theta - \alpha/2) \sin \frac{n\alpha}{2}$$

The factor $\sin \frac{n\alpha}{2}$ is simply the coil pitch factor for the harmonic of order n, and is normally known as K_p.

The coefficients b_n are found by carrying out the harmonic analysis of the current-density distribution in the usual way. As shown in Fig. 13, the current density may be considered as a number of 'impulse' or 'Dirac' functions, and this greatly

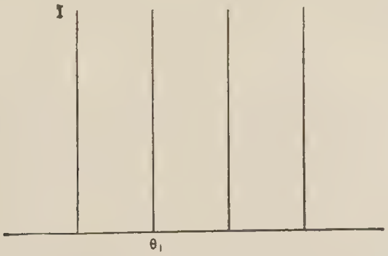

Fig. 13.—Current distribution of conductors each carrying current I.

facilitates the integration. On carrying out this integration it follows that b_n is proportional to $\Sigma \cos n\theta_1$, where the summation is made over all angles, θ_1, at which a conductor of the phase considered is situated.

It is now shown that $\Sigma \cos n\theta_1$ is proportional to K_d, the spread factor of the winding for a flux-density wave of the form $a \cos(n\theta + \alpha)$.

From Fig. 14 it may be seen that the spread factor is

$$\frac{OA_k}{OA_1 + OA_2 + \ldots}$$

which, since $OA_1 = OA_2 =$ etc., may be written

$$\frac{OA_k}{kOA_1}$$

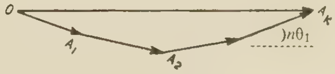

Fig. 14.—Vector diagram for the spread factor of a winding.

Since the angles are measured relative to the axis of symmetry,

$$\frac{OA_k}{OA_1} = \Sigma \cos n\theta_1$$

Hence b_n is proportional to K_d and the current-density distribution due to unit current in one phase is proportional to $\Sigma a_n \sin n\theta$, where $a_n = K_d K_p$ for the harmonic of order n.

(12.5.2) Harmonics in the Flux-Density Distribution of a 3-Phase Machine.

In a 3-phase winding, the currents in the three phases are sinusoidal in time and differ by 120° in phase. The combined current distribution due to all three phases when the current in one of the phases is $\sin \omega t$ is given[12] by

$$\tfrac{3}{2} \sum_{n=1,4,7\ldots} a_n \cos(n\theta - \omega t) - \tfrac{3}{2} \sum_{n=2,5,8\ldots} a_n \cos(n\theta + \omega t)$$

(12.5.3) Flux-Density Distribution and Force Distribution.

The m.m.f. distribution is obtained by integrating the above current distribution pattern with respect to distance round the stator bore. Neglecting variations in permeance the flux-density distribution is proportional to the m.m.f. distribution and is therefore given by

$$\sum_{n=1,4,7\ldots} \frac{a_n}{n} \sin(n\theta) - \omega t - \sum_{n=2,5,8\ldots} \frac{a_n}{n} \sin(n\theta + \omega t)$$

The magnetic force of attraction at the stator bore is proportional to the square of the flux density and is therefore proportional to

$$\left[\sum_{n=1,4,7\ldots} \frac{a_n}{n} \sin(n\theta - \omega t) - \sum_{n=2,5,8\ldots} \frac{a_n}{n} \sin(n\theta + \omega t)\right]^2$$

This expression may be regarded as a superposition of various force waves. The waves which are most likely to cause vibration of the stator are those of longest wavelength.

From the above expression the term of longest wavelength is given by

$$\left(\frac{a_1 a_2}{1 \times 2} + \frac{a_4 a_5}{4 \times 5} + \frac{a_7 a_8}{7 \times 8} + \ldots\right) \cos(\theta + 2\omega t)$$

which is a force wave whose frequency is twice the fundamental frequency.

In this expression, the dominant term is usually the term involving a_p, the amplitude factor of the fundamental, and is either $\left[\frac{a_p a_{p+1}}{p(p+1)}\right]$ or $\left[\frac{a_p a_{p-1}}{p(p-1)}\right]$.

Frequently a winding is used in which $n_1 = n_2$, so that all the even harmonics are zero, i.e. $a_2 = a_4 = a_8 \ldots = 0$.

In this case the above force-wave harmonic vanishes and the term of longest wavelength is that involving $\cos(2\theta - 2\omega t)$. The amplitude of this is given by

$$\left(\frac{a_1^2}{2} + \frac{a_5 a_7}{5 \times 7} + \frac{a_{11}a_{13}}{11 \times 13} + \ldots\right)\cos(2\theta - 2\omega t)$$

This is a force wave of twice fundamental frequency, and again the dominant term is usually the one involving a_p and either a_{p+2} or a_{p-2}.

(12.6) Interchange of Conductors at the Ends of Phase Bands

If in the equivalent positive phase band of phase I, Fig. 1, conductors D and K are removed and conductors E and L are added, we have

$$\Sigma(\cos\phi_1) = \frac{\sin(n_1\theta)}{\sin\theta} - 2\cos[(n_1 - d)\theta] + 2\cos[(n_1 + e)\theta]$$

Similarly, for the equivalent negative phase band,

$$\Sigma(\cos\phi_2) = (-1)^{nX}\left\{\frac{[(\sin(n_2\theta)]}{\sin\theta} + 2\cos[(n_2 + d)\theta] - 2\cos[(n_2 - e)\theta]\right\}$$

so that for 'nX even'

$$\Sigma(\cos\phi) = \Sigma(\cos\phi_1) - \Sigma(\cos\phi_2)$$

$$= \frac{2\cos\left[(n_1+n_2)\frac{\theta}{2}\right]\sin\left[(n_1-n_2)\frac{\theta}{2}\right]}{\sin\theta} - 4\cos\left[(n_1+n_2)\frac{\theta}{2}\right]\cos\left[(n_1-n_2-2d)\frac{\theta}{2}\right] + 4\cos\left[(n_1+n_2)\frac{\theta}{2}\right]\cos\left[(n_1-n_2+2e)\frac{\theta}{2}\right]$$

$$= 2\cos\left[(n_1+n_2)\frac{\theta}{2}\right]\left\{\frac{\sin\left(\frac{m\theta}{2}\right)}{\sin\theta} - 2\cos\left[(m-2d)\frac{\theta}{2}\right] + 2\cos\left[(m+2e)\frac{\theta}{2}\right]\right\}$$

where $m = (n_1 - n_2)$ and m, d and e can have any odd value.

In some cases sufficient reduction in the lowest force sub-harmonic is obtained by making $d = e$; then

$$\Sigma(\cos\phi) = 2\cos\left[(n_1+n_2)\frac{\theta}{2}\right]\sin\left(\frac{m\theta}{2}\right)\left[\frac{1}{\sin\theta} - 4\sin(d\theta)\right]$$

Thus the effect of making the interchange is to multiply the value of $\Sigma(\cos\phi)$ before making the change by the factor $K_f (= 1 - 4\sin\theta\sin d\theta)$ for any value of m.

Similarly, for nX odd it can be shown that the multiplying factor is the same.

(12.7) Effect on Harmonics of Rotating One of the Two Repeatable Sections of the Stator Winding

For simplicity, a winding with two repeatable sections is considered, so that the stator consists of two identical halves.

In the pattern under the 'equivalent pole pair' the angle between adjacent positions is given by $2\theta = nX2\pi/N$, where $(n = 1)$ is the lowest harmonic present, and N is the number of slots in one of the two repeatable sections. A rotation of this pattern through S positions is equivalent to moving each of the stator conductors through SX slots. This has the same effect on the winding factor of harmonics which already exist as that of rotating one of two collinear vectors through an angle $2S\theta$. The result is that the amplitude of the harmonic is multiplied by $|\cos S\theta|$.

The two repeatable sections, considered separately, would each produce harmonics corresponding to values of n equal to $\frac{1}{2}$, $\frac{3}{2}$, $\frac{5}{2}$, etc., in the above formula for 2θ, but because the two sections are identical, except for an angular displacement in space of 180°, these harmonics cancel in the complete machine. If one of the two repeatable sections is shifted through S positions under the equivalent pole-pair, these harmonics, given by $n = \frac{1}{2}$, $\frac{3}{2}$, $\frac{5}{2}$, etc., do exist and the amplitude of such harmonics is equal to the amplitude of the harmonics produced by one repeatable section multiplied by $|\sin S\theta|$, because the effect is that of rotating one of two equal and opposite vectors through an angle $2S\theta$.

If a winding has more than two repeatable sections, the effect of rotating one or more of them can be dealt with in a similar manner.

(12.8) Shift of Winding Pattern

Since neighbouring conductors in the diagram (Fig. 2) are actually X slots apart on the machine, a shift of S places under the equivalent pole is the same as a shift of SX slots in the actual machine.

The number of slots per pole-pair is N/P, so that using the relation $xN = PX \pm 1$, it is seen that

$$\frac{SXP}{N} = S\left(x \pm \frac{1}{N}\right) = Sx \pm S/N$$

Since S/N is usually small, the sequence is shifted Sx pole-pairs.

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CAPACITORS FOR DISCHARGE-LIGHTING CONTROL CIRCUITS

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(*The paper was first received* 31*st December*, 1957, *and in revised form* 5*th March*, 1958.)

SUMMARY

A short Section on manufacture and materials outlines typical features in the construction of a capacitor for discharge lighting and refers briefly to common practice in the choice of impregnating material and vacuum processing conditions.

Under the general heading of dielectric life and breakdown the probable effects of conducting paths in tissue are mentioned. The importance of discharge-inception stress is emphasized, reference being made to discharge-inception measurements on capacitors impregnated with petroleum jelly and with pentachlorodiphenyl, and to the interim results of long-term accelerated tests on pentachlorodiphenyl-impregnated capacitors. A suggestion that electrochemical deterioration is not a major factor contributing to failure of paper capacitors under a.c. stress is discussed. A note on chemical decomposition of impregnants during breakdown is followed by discussion of the use of fusing to reduce or eliminate such chemical effects.

A final Section discusses power factor as a criterion of capacitor quality.

(1) INTRODUCTION

The increasing use of interior and exterior discharge lighting during recent years has provided an excellent opportunity to observe the performance of large numbers of impregnated-paper capacitors at comparatively high ambient temperatures and r.m.s. voltages in the range 230–440 volts.

The users of the larger a.c. capacitors for heavy-current applications are normally prepared to accept the cost of almost complete reliability. With discharge-lighting circuits, great reliability is also required, but the demand for low cost is insistent. Consequently, the capacitor engineer has a very particular interest in the relationships between reliability, mode of construction and manufacturing conditions.

After a brief note on manufacture and materials, the paper discusses some important factors which influence the reliability of this class of capacitor.

(2) MANUFACTURE AND MATERIALS

An 8 μF 250-volt (a.c. working) capacitor for shunt power-factor correction still appears to be the most common in fluorescent-lighting control equipment and will be taken as an example. Typically, the winding of paper and foil consists of two aluminium foils, 6–8 μ thick, and two pairs of kraft-tissue interleavings, commonly 10 μ thick. Pure-tin or tinned-copper connector tabs are inserted during winding of the roll, and connections are made to the capacitor terminals either by spot welding or soldering. After being vacuum-dried, degassed and impregnated, the wound unit is sealed in its metal container.

(2.1) Types of Impregnant

Three commonly used alternatives for a.c. paper capacitor impregnation are petroleum jelly, mineral oil and chlorinated diphenyl, and there are many possible variants using these three basic materials. All three may be used with or without stabilizers. The degree of chlorination of chlorinated diphenyl may be varied to suit the operating temperature range required for the capacitor, and this range may also be varied by mixing chlorinated diphenyl with trichlorobenzene.

However, the majority of capacitors for lighting circuits are impregnated with either petroleum jelly or pentachloro-diphenyl, with no additives, despite the wide variety of trade names used for impregnants.

(2.2) Vacuum-Drying and Degassing

Little scientific information on the vitally important vacuum-drying of paper capacitors has been published in England, although a systematic investigation of the relationship between residual moisture in capacitor windings and the associated vacuum conditions has been made in Germany. Hochhausler[1,2] claims that water can be removed completely from capacitor windings, as evidenced by a drop test on the vacuum-tight container at the end of a drying cycle,* by evacuating until the equilibrium total pressure in the container has reached approximately 0·001 mm Hg. Even with the most rapid diffusion-pumping methods, drying to this degree may require 7–10 days, since the last stages of moisture evolution are controlled by the rate of diffusion through the windings.

The matter of great interest to the designer of mass-produced capacitors is the relationships between the residual moisture content and capacitor life and reliability. All accelerated tests on capacitors have their limitations, and after first commencing work with any given vacuum conditions and a particular impregnant, a period probably exceeding 10 years is needed to arrive at reliable conclusions on factors affecting life.

With discharge-lighting capacitors it is at present common practice to dry windings at a vacuum of about 0·1 mm Hg and for a total period of evacuation not exceeding 2 days.

(3) DIELECTRIC LIFE AND BREAKDOWN

(3.1) Conducting Paths in Tissue

It is well known that capacitor tissue contains a small number of conducting paths which paper manufacturers have so far been unable to exclude completely. The number of paths present in a given area of tissue increases with reduction of tissue thickness. These weaknesses in the tissue are most probably responsible for the small number of capacitor breakdowns which occur on production proof tests and may also be very largely responsible for a few breakdowns of 2-interleaving capacitors in service.

An investigation[3] of the chemical nature of conducting paths has provided strong evidence that they are normally carbonaceous (probably coke particles), and has shown that their diameters vary up to approximately 100 μ. Thus, in some cases they are sufficiently small to be embedded in a single tissue, while in others they may partially penetrate the adjacent tissue. Owing to their presence, a capacitor designer discounts one interleaving when choosing total paper thickness to withstand a given stress. With capacitors stressed below ionization inception it seems reasonable to suppose that an early failure arises from the

Written contributions on papers published without being read at meetings are invited for consideration with a view to publication.
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* A test in which the container, when sealed off, showed no loss of vacuum other than any slight leakage measured on the container before it was loaded for the vacuum-drying and degassing cycle.

exceptionally high stressing of average-quality dielectric adjacent to a conducting path rather than from a general deterioration of the whole dielectric.

An important question for the capacitor engineer is: what, for any specific type of capacitor, is the optimum over-voltage test needed to eliminate capacitors with over-stressed regions in the dielectric while not damaging the normally stressed portions? The time required to complete investigations of this type may result in the adoption of more rigorous proof-testing conditions with a certain degree of initial risk. Alternatively, a very small incidence of short-term failures—about 0·01%—may be considered acceptable in the case of 2-interleaving capacitors. A capacitor designer may be required to compromise on reliability to this extent since, in 250-volt a.c. capacitors, thinner tissue to give three instead of two interleavings is approximately twice as costly, weight for weight.

(3.2) Capacitor Life in Relation to Discharge-Inception Stress

The importance of discharge-inception stress in a.c. paper capacitors has been underlined by work at the Electrical Research Association, and it has been concluded that eventual failure is inevitable when discharges are allowed to persist in any dielectric. Discharge inception has been found to occur in the region 300–400 volts r.m.s. in individual capacitor windings with twin 12 μ interleavings impregnated with petroleum jelly. In similar pentachlorodiphenyl-impregnated windings which have received similar drying and degassing treatment to a final medium vacuum between 0·1 and 0·5 mm Hg before impregnation, discharge inception occurs between 1 and 1·3 kV r.m.s.

The writer is unable to quote figures for mineral oils from direct experience, but results of E.R.A. work[4] have been published. It may be noted that the figures quoted above were obtained from measurements using the type of discharge detector (see Section 8) used in the E.R.A. work on mineral-oil-impregnated capacitors and that similar precautions were taken in distinguishing between contact discharges and discharges in the main body of the capacitor.

The melting and resolidification of petroleum jelly during the temperature cycles commonly encountered by a capacitor in a fluorescent-lighting fitting undoubtedly favour void formation, and an impregnant which is fluid over the whole working temperature range seems a logical choice for this type of application.

Direct observation of the performance of pentachlorodiphenyl-impregnated capacitors on accelerated endurance tests in the laboratory confirms that a period of many years must elapse before life distribution curves can be completed. Table 1 shows the interim results of an extended accelerated test at 400 volts r.m.s. and 80° C, on a batch of nine pentachlorodiphenyl-impregnated capacitors designed for normal operation at 275 volts r.m.s. (maximum) and temperatures between 0 and 70° C.

These units are wound with twin interleavings of 12 μ kraft tissue having a density of 1·2, and connections are made by a single pair of tinned-copper tags placed centrally in the winding. The units were dried and degassed under medium vacuum conditions (0·1–0·5 mm Hg) before impregnation with pentachlorodiphenyl. The impregnant had been given Fuller's-earth treatment and medium vacuum drying and degassing. No stabilizer was added.

It will be noted that, with nominal tissue thickness, the peak stress in the accelerated test is 23·6 volts/μ. This particular test was originally commenced as one of a number of type tests at approximately 1·5 times working voltage, but was continued as an accelerated life test. Its main purpose was to indicate the order of life to be expected from a capacitor with twin 12 μ interleavings, vacuum processed under specific conditions, and at the time of writing eight capacitors have survived 26 000 hours, with less than 1% change in capacitance after 20 000 hours. From later tests on similar capacitors where measurements of discharge inception, power factor, capacitance and insulation resistance are made before and at intervals during the tests, it is hoped to make some assessment of pentachlorodiphenyl as an impregnant in capacitors treated under specified vacuum conditions.

Table 1

Accelerated Test on Nine Pentachlorodiphenyl-Impregnated Paper Capacitors rated at 8 μF ± 10%, 275 volts r.m.s. (max.)

Test Conditions: 400 volts r.m.s. and 80° C

Capacitance		Insulation resistance × Capacitance corrected to 20° C	
Before test	After 20 000 hours	Before test	After 20 000 hours
μF	μF	ohm-farads $\times 10^{-3}$	ohm-farads $\times 10^{-3}$
8·28	8·34	12·5	15·3
8·30	8·36	12·6	15·5
8·30	8·36	13·6	15·3
8·32	8·38	14·4	14·5
8·32	8·39	12·8	14·0
8·24	8·32	11·6	15·0
8·30	*	12·2	*
8·28	8·33	14·3	14·9
8·30	8·37	13·0	14·8

* Breakdown at 1 500 hours.

Any work which provides a basis for deciding how closely the capacitor peak working stress may safely approach the lowest discharge-inception stress associated with a particular design and set of manufacturing conditions is of value. However, an important practical limitation exists. In present-day practice it is usual to permit stressing of impregnated-paper capacitor dielectrics at 10–14 volts/μ r.m.s. during normal operation. Since, in capacitors with twin 12 μ interleavings impregnated with pentachlorodiphenyl, it has been found that the discharge-inception stress is about three times the peak working stress, it might appear possible to reduce substantially the thickness of interleavings. Unfortunately, such reduction involves an increase in the number of conducting paths and hence possibility of an increased number of early failures.

(3.3) Electrochemical Deterioration of Impregnated-Paper Capacitors under A.C. Stress

The writer has no direct evidence from laboratory experiments concerning electrochemical deterioration in a.c. paper capacitors, but from service experience there are some grounds for concluding that it is not an important factor, provided that the voltage stress is well below discharge inception.

It is noteworthy that, for capacitors impregnated with pentachlorodiphenyl (which gives a high discharge-inception voltage) and provided with a sufficient number of interleavings, to avoid early failure due to conducting paths, the service failure rate has been negligible* in a particular case. This seems to indicate the absence of serious electrochemical action, particularly since this absence of failures has been observed with capacitors vacuum-dried to approximately 0·1 mm Hg—which definitely leaves some residual moisture in the dielectric. Such moisture would assist any processes of electrochemical deterioration.

* This refers to a case where no failures have been reported, but it cannot necessarily be assumed that none have occurred. The capacitors have three 12 μ interleaving tissues and are rated at 400 volts r.m.s. The total number in service is 150 000 and the maximum time in service is 4 years.

A specific comparative example was the observed superiority in service of certain capacitors impregnated with pentachlorodiphenyl over equivalent capacitors impregnated with petroleum jelly—equivalent in the sense that the same materials were used for windings apart from the impregnant and the same medium-vacuum drying process was used. Owing to differences in permittivity, the overall sizes differed for the same capacitance and working voltage, as did the outer casings and terminal bushings. From the relative figures for discharge inception quoted in Section 3.2 it would be expected that under certain circuit conditions discharges could occur in the capacitors impregnated with petroleum jelly but would probably not occur in the similar capacitors impregnated with pentachlorodiphenyl.

The interim life-test results reported in Section 3.2 at least begin to suggest, in a particular case, that no major processes of electrochemical deterioration have occurred. It is well established that, under d.c. stress, polar impregnants such as pentachlorodiphenyl undergo electrochemical deterioration, and some workers consider that it takes place under low-frequency a.c. stress.[5] It has been noted[6] that, although a slow enough electrochemical reaction may be reversible with each half-cycle, many reactions are sufficiently fast to proceed under 50 c/s a.c. stress as well as d.c. stress.

(3.4) Chemical Decomposition of Impregnants during Capacitor Breakdown

It is proposed to refer only very briefly to forms of chemical decomposition which have in some cases been associated with the electrical failure of capacitors of this class. It is fairly widely known that the occurrence of discharges in petroleum jelly can result in release of free hydrogen, in addition to causing progressive deterioration of the dielectric, leading to ultimate breakdown. In some instances, hydrogen formation has set up sufficient internal pressure to rupture a metal container (particularly along a soldered seam) before eventual failure of the winding. Some instances have also been observed where electrical failure of a chlorinated-diphenyl-impregnated capacitor has been accompanied by the formation of decomposition products. Internal pressure has sometimes been sufficient to swell or rupture the container. Experience has shown pentachlorodiphenyl in capacitors to be very stable when stressed below discharge inception. Decomposition can occur under the arcing conditions accompanying some forms of dielectric breakdown.

In production over-voltage tests a very small percentage of breakdowns occurs which, over a number of years, becomes a significant number of capacitors. From this source of information it was possible to observe that, where the fault current was limited to a maximum of 6–7 amp in individual units, failure was never accompanied by appreciable decomposition of the pentachlorodiphenyl impregnant.

Suitable fusing of the control circuit or the capacitor will ensure that any failure will not be associated with leakage of impregnant. In some cases, provision is made incidentally by the existing fusing of fittings. In other cases the nature of the control circuit is such as to limit the fault current in a failed capacitor.

A practice sometimes adopted is to provide internal fuses in capacitors rated at 250 volts a.c. working, and intended for shunt power-factor correction. This provision is made, in some cases, with both chlorinated-diphenyl- and petroleum-jelly-impregnated capacitors. It should be remembered that an internal fuse tends to conceal any incidence of failure with shunt correction, since a breakdown becomes an open-circuit and the change of power factor may not be noticed. It may also be noted that an internal fuse in a chlorinated-diphenyl-impregnated capacitor eliminates the possibility of decomposition of impregnant under heavy fault current only if so designed that an arc to the case following rupture of the fuse is impossible.

(4) POWER FACTOR

Some specifications for capacitors for discharge-lighting circuits include such a statement as 'power factor better than 0·4%' under specified conditions. It is important to note that, with large individual capacitor windings such as are under discussion, the power factor may have an appreciable component due to the resistive loss of the aluminium foil, since geometrical considerations may require a winding with a long length of narrow foil. In extreme cases this resistive loss may increase the power factor by as much as 0·0025 at 50 c/s. At 1 kc/s this would increase to 0·05 and completely swamp the dielectric component. A high power factor is therefore not necessarily an indication of bad quality in the dielectric. This degree of resistive loss may be avoided by fitting more than one pair of connecting tabs, but it is doubtful whether such a manufacturing complication is justified, since its heating effect is negligible at 50 c/s. The resistive component of the power factor is easily calculable for any given design and frequency, and should be subtracted from the measured power factor to obtain the dielectric dissipation component.

It has been suggested that the expression 'power factor' should be discontinued in specifications for large capacitors in favour of the 'dielectric dissipation factor' as defined above. It might be maintained that the specification of both is desirable where the capacitors concerned are likely to be used in circuits with an appreciable harmonic content, or with supply frequencies above 50 c/s.

It is significant that, from the writer's experience, capacitors of varying size and geometrical shape have dielectric dissipation factors consistently close to each other, and that any error in vacuum-drying processes is immediately reflected in a significant increase in this factor, which is therefore a valuable control measurement.

Some authorities in the past have favoured the use of insulation resistance as the best criterion of quality, but the writer's experience is that this figure is subject to much greater variation, particularly from batch to batch, probably owing mainly to the difficulty of correctly applying the large corrections for temperature which are necessary. Temperature has only a small effect on power factor compared with that on insulation resistance.

(5) CONCLUSIONS

From this short survey the suggestion emerges that there are two distinct considerations in assessing life and reliability of paper capacitors of this class.

The presence of conducting paths in tissue may be largely or wholly responsible for any small incidence of short-term failure in capacitors whose dielectric as a whole has good properties. If it is desired to eliminate this type of failure without taking the obvious but costly step of increasing the interleavings, investigation of short-duration tests at voltages higher than those now commonly used for proof tests seems a suitable approach. Discharge inception and extinction stresses are obviously of great importance in connection with such work. A considerable period would be required, and the performance of many thousands of capacitors must be studied, to complete such an investigation.

The quality of the dielectric as a whole and its behaviour under the stresses which it has been designed to withstand are the aspects of capacitor reliability which will determine the general form of a life-distribution curve for capacitors in service.

The first indications from accelerated tests over a limited period (3 years) are that, with a stable impregnant which is fluid throughout the working temperature range, and with units stressed well below discharge inception, an average life quite sufficient for the applications concerned can be expected.

(6) ACKNOWLEDGMENTS

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(8) APPENDIX

Discharge-Inception and -Extinction Stresses

Whenever the terms discharge-inception stress (or voltage) and discharge-extinction stress (or voltage) are used in the paper they refer to the results of measurements made with the first commercial discharge detector based upon methods used in E.R.A. work[7] in this field of measurement. In this instrument the smallest detectable discharge magnitude is directly proportional to the square root of the capacitance of the specimen.

In the paper, specific results are quoted only for 8 μF capacitors constructed with twin 12 μ tissue interleavings, since 8 μF is a very common capacitance in discharge-lighting circuits. The discharge-inception voltages given might more cautiously be termed 'discharge-appearance voltages measured with this particular instrument at its particular sensitivity when the input circuit is matched to an 8 μF capacitor'. However, precise absolute values are not so important when it is intended to compare discharge-inception values for capacitors of the same value but impregnated with different materials.

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